ELECTRICAL ENGINEERING

Registered U. S. Patent Office

DECEMBER
1942

The Cover: Electric welding plays a tremendous part in war production.
Photo courtesy of Westinghouse Electric and Manufacturing Company

Lightning Protection of Hazardous Structures .... G. D. McCann . . . 591
Ultrahigh-Frequency Transmission in Wave Guides .... L. A. Ware . . . 598
An Electronic Circuit for Studying Hunting . . . M. J. DeLerno, R. T. Basnett . . . 603
More Mica From the Western Hemisphere 607
Switchgear Practice in Europe and America .... J. B. MacNeill . . . 609
I Am an American .... Aram Boyajian . . . 614
Miscellaneous Short Item: Induction Heating Saves Tin, 613
Institute Activities . . . 616
Of Current Interest . . . 626

TRANSACTIONS SECTION

(Follows EE page 638; a preprint of pages 835–892 of the 1942 volume)

Vi Energy-Flow Postulate .... Joseph Slepian . . . 835
Quebec Power-System Interconnection .... W. R. Way . . . 841
Transformer Ring-Bus Characteristics .... G. W. Bills, C. A. MacArthur . . . 848
Inverse Functions of Complex Quantities .... Herbert B. Dwight . . . 850
Transformer Differential Protection Problems .... P. W. Shill . . . 854
Canadian Circuit-Breaker Installations .... H. W. Haberl, R. A. Moore . . . 859
Determination, Standardization of A-C Wave Form .... Frederick Bedell . . . 864
High-Voltage Axial Air-Blast Circuit Breakers .... Armin K. Leuthold . . . 869
A-C Network Analysis Using Resistance Networks .... Waldo E. Enns . . . 875
120-Kv Oil-Filled Cables in Canada .... D. M. Farnham, O. W. Titus . . . 881
New Single-Pole Service Restorer .... E. E. Tugby . . . 889

G. Ross Henninger
Editor (on leave)
Floyd A. Lewis
Acting Editor
F. A. Norris
Business Manager
C. A. Graef
Advertising Manager

Statements and opinions given in articles and papers appearing in *Electrical Engineering* are the expressions of contributors, for which the Institute assumes no responsibility. ¶ Correspondence is invited on all controversial matters.

Published Monthly by the

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Founded 1884

VOLUME 61
NUMBER 12

HAROLD S. OSBORNE, President
H. H. HENLINE, National Secretary

PUBLICATION COMMITTEE: Howard S. Phelps, *chairman;* C. E. Dean; F. Malcolm Farmer; H. H. Henline; K. B. McEachron; John Mills; A. G. Oehler; P. H. Pumphrey; H. H. Race; S. B. Williams

HIGH LIGHTS • •

Air-Blast Breakers. Two papers on air-blast circuit breakers in this issue describe: (1) short-circuit tests on Canadian installations ranging in rating from 4 to 230 kv (*Transactions pages 859–63*); principles of design and operation of high-voltage air-blast circuit breakers, and the construction of two air-blast breakers rated, respectively, 150 and 220 kv (*Transactions pages 869–75.*)

A-C Wave Form. With the increasing necessity of wave-form standardization, different standards have been developed in different fields, as in power, communication, and insulation. While standards, once set up, should remain fairly stable, they should also be subject to review and occasional change to keep in step with technological advances (*Transactions pages 864–8*).

Ring-Bus Characteristics. Network-analyzer studies on a transmission system having a three-winding transformer ring bus and on the same system using a standard high-voltage bussing arrangement proved that transient stability limits are higher and fault currents lower for the three-winding transformer ring-bus system (*Transactions pages 848–9*).

A-C Network Analysis. A conventional d-c calculating board may be used to solve complex problems of load distribution and voltage conditions for a-c networks. For short-circuit work this method can be used to advantage where (1) the resistance of the network circuits is appreciable, so that a network setup representing reactance does not give sufficiently accurate results, and (2) a setup representing impedance is inaccurate because of differences in ratio of X to R for the circuits forming the network (*Transactions pages 875–80*).

Single-Pole Service Restorer. A single-pole service-restoring device utilizes an application for the prestoring of operating energy and the restoration of the utilized operating energy after transient fault conditions (*Transactions pages 889–92*).

Lightning Protection. Basic principles of adequate protection for hazardous structures are gained from experience with similar shielding of electric-power systems, says a lightning engineer. Proper location of masts or ground wires, necessary sizes of conductors, necessary clearances between parts of the shielding system and the structure being protected, and ground resistances obtained with different types of grounds are considered (*pages 591–7*).

Power-System Interconnection. Interconnection of three important hydroelectric systems in Canada, made necessary by war needs, involved combining power resources of nearly 4,000,000 horsepower and has increased the combined firm power capacity far beyond the sum of the capacities of the individual systems when they were operated independently (*Transactions pages 841–7*).

120-Kv Oil-Filled Cables. Design, manufacture, and installation of a 120-kv oil-filled cable system recently installed in Canada as a link in a large interconnection scheme are described in a paper in this issue. Functions of the various sections and reasons for choice of method of effecting the interconnection are explained (*Transactions pages 881–9*).

Inverse Functions. Formulas for inverse functions of complex quantities, such as $\sin^{-1}(x \pm iy)$, are of use in calculations for transmission circuits, particularly in connection with communication circuits (*Transactions pages 850–3*).

Transformer Differential Protection. Tolerances in the selection of current transformers and factors affecting the selection of relays for that purpose are considered in a discussion of the protection of interconnected wye and zig-zag transformers and Scott-connected transformers by the usual current-differential method (*Transactions pages 854–8*).

Correction. In the first paragraph of column two of M. Hochstadter's letter to the editor on compression and oil-filled cable versus gas-filled cable (*EE, November '42, p. 586*) the second and third sentences should read: "European engineers are certainly biased in this matter as they believe in the necessity of a lead sheath. In fact they have hitherto abhorred designs . . ."

Energy Flow in Electric Systems. Examples of the commonly used *Vi* energy-flow postulate show it to be not generally valid, but by adding a simple term it can be made equally valid with other valid energy-flow postulates. The application of this corrected energy-flow postulate is demonstrated (*Transactions pages 835–41*).

Hunting. An electronic circuit for determining power-angle oscillations in synchronous machines during periods of hunting is described in the AIEE national Branch prize paper for the academic year ending June 30, 1941. The method is said to have certain advantages over previous methods (*pages 603–06*).

Developments in Switchgear. The history of switchgear apparatus in Europe and America and the present trends in development of switchgear on both continents have been analyzed in the light of current needs and facilities. The requirements of each of three major classes of switchgear are considered individually (*pages 609–13*).

Wave Guides. Methods for determining the shape and size of wave guides for ultra-high-frequency transmission and the mode of transmission to be used are discussed in an article in this issue. Rectangular and cylindrical guides as well as transverse-electric and transverse-magnetic modes of transmission are described (*pages 598–603*).

Mica. Domestic resources of mica, at present an urgently needed raw material, are being developed. In an article in this issue the chief mica sources are pointed out and the production and classification of the various types of mica are discussed (*pages 607–09*).

"I Am an American." An AIEE member who is a naturalized American citizen of Armenian background tells what being an American means to him (*pages 614–15*).

Coming Soon. Among the special articles and technical papers now in preparation for early publication in *Electrical Engineering* are: an article on inventions and the engineer by W. E. Crawford (F '36); a description of the improvement of the lighting in the auditorium of the Engineering Societies Building by S. W. Bruun (A '40); a discussion of power facilities and problems in South America by R. P. Crippen; an outline of power-line transposition practices by F. Von Voigtlander (M '38); an analysis of a practical equivalent circuit for a five-winding transformer by L. C. Aicher, Jr. (A '37); a report on the correlation of overvoltages with system-grounding impedance by an AIEE group; a study of hunting and self-excitation of a 300-mile transmission line by R. B. Bodine (A '37), C. Concordia (M '37), and Gabriel Kron (A '30); a discussion of equivalent circuits for oscillating systems by Gabriel Kron (A '30); a discussion of a polyphase adjustable-speed motor and its control circuit by A. G. Conrad (M '40), S. T. Smith (Enrolled Student), and P. F. Ordung (Enrolled Student); an analysis of rating methods for intermittent loads by R. E. Hellmund (F '13, deceased); a consideration of the dielectric-recovery characteristics of large air gaps by G. D. McCann (A '38) and J. J. Clark (A '40); a study of representative circuits with and without series-capacitor compensation by H. A. Peterson (M '41) and T. W. Schroder (A '37); a description of an instrument for the determination of contact making and breaking time by Walther Richter (F '42) and William H. Elliot (A '40), and presentation of a method of calculating performance on tapped-winding capacitor motors by P. H. Trickey (M '36).

Electrical Engineering: Copyright 1942 by the American Institute of Electrical Engineers; printed in the United States of America; indexed annually by the AIEE, weekly and monthly by *Engineering Index*, and monthly by *Industrial Arts Index;* abstracted monthly by *Science Abstracts* (London). Address changes must be received at AIEE headquarters, 33 West 39th Street, New York, N. Y., by the 15th of the month to be effective with the succeeding issue. Copies undelivered because of incorrect address cannot be replaced without charge.

Lightning Protection of Hazardous Structures

G. D. McCANN
ASSOCIATE AIEE

Separately mounted shielding systems, such as vertical diverter masts or ground wires, are best for most hazardous buildings, because they produce adequate shielding and sufficient clearances to prevent sparking. Cost of materials and installation are less than with the use of lightning rods, since conductors for the masts and ground wires are smaller, and corrosion-resisting steel wire can be used instead of copper. Location of masts or ground wires, sizes of conductors, necessary clearances, and ground resistances are considered in this article.

OUR present knowledge of the characteristics of natural lightning and the protection of electric-power systems, gained from operating experience and the extensive investigations conducted during the past 25 years, has provided considerable information which can be applied to the protection of structures other than transmission lines and substations. Lightning rods have been used for many years for a wide variety of buildings and other structures and have considerably reduced lightning damage. As the realization has been reached that their true function is to protect against direct strokes and not to prevent them, and as the importance of obtaining good grounds has become better appreciated, the efficiency of this type of protection has been increased. Experience indicates that most of the common types of structures, such as rural farm buildings and the like, can be protected to a sufficient degree with the conventional application of lightning rods mounted directly on the structure, as long as they are correctly installed in accordance with the "Code for Protection Against Lightning."[1] The degree of protection necessary depends upon property value, vulnerability or hazard, and probability of direct strokes. The best possible protection is of considerable importance for structures in which petroleum products, inflammable gases, and explosives are being handled or stored. A very large number of these are being constructed at the present time throughout the country. Protection methods which have been found sufficient for ordinary buildings are not necessarily adequate for these. It is felt that more general application of methods applied to power systems will result not only in better protection, especially for these hazardous structures, but also a saving in critical materials and labor as compared to present methods of protecting such structures.

PROBABILITY OF DIRECT STROKES

The variation of lightning conditions throughout the United States is best shown by the isoceraunic map of

---

G. D. McCann is central-station engineer with Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

Figure 1, which gives the average over a 30-year period of the number of thunderstorm days each year occurring in different parts of the country. For the eastern part the average is about 30 storm days per year, a value that is usually taken as a basis for comparison. The frequency and severity of storms varies considerably from year to year and in any region with local conditions of terrain and storm paths. However, it is possible to determine the order of magnitude of the frequency with which objects are likely to be struck and the factors which influence this. Through lightning field investigations[3] such records have been obtained for structures in regions of isoceraunic storm levels lying between 25 and 40. These data are given in curve *A* of Figure 2 and show that below about 500 feet the probability of strokes is about proportional to height. Supplementary data[3] obtained from laboratory model studies have determined the effect of the horizontal dimensions of the structure. The area it covers is the principle factor as shown by curve *B* of Figure 2. For buildings whose horizontal dimensions are of the same order of magnitude as their height, area has little effect, and curve *A* can be used directly. Objects very low in relation to the area which they cover, such as oil reservoirs, are not likely to attract strokes from a sky area larger than their own area. The probability of direct strokes is directly proportional to the area of the structure and the number of strokes which emanate per unit area of sky. Data obtained on strokes to transmission lines together with laboratory studies[3] indicate that an average figure for the number of strokes emanating each year from a square mile of sky area in regions of isoceraunic storm level of 25 to 40 is about 9.5. For estimating purposes the expectancy of strokes in regions varying considerably in the number of storms than that to which the curves of Figure 2 apply can be obtained by using the direct ratio between the respective isoceraunic levels.

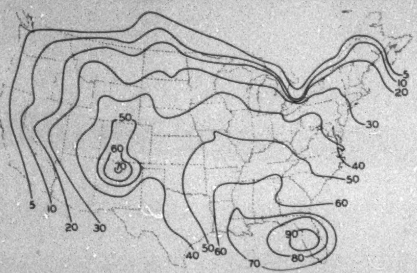


Figure 1. Annual isoceraunic map showing the number of thunderstorm days per year (Alexander[2])

The data of Figure 2 apply to structures situated on essentially level terrain and sufficiently far from other objects so that they will not shield it. The shielding provided by such adjacent objects is treated in a later section. Curve 2*b* shows that an average sized structure 25 feet in height may be struck as often as once every 30 or 40 years, while one of large extent having an area of, say, 40,000 square feet, about once every ten years. Some of the modern ordnance plants may have several hundred buildings so that there is a good likelihood of several in each plant being struck each year. The oil reservoirs of tank farms frequently cover as much as one tenth to one quarter of a square mile. Thus, there is a probability that one to three direct strokes would occur each year if they were not adequately shielded.

SHIELDING

Some types of structures, such as metal tanks, are essentially self-protected in that they provide an adequate continuous conducting path for a stroke which would contact them. The most complete protection of any structure or any portion of a structure for which this is not the case would be obtained by providing a complete metallic shell around it which would be struck instead. This is seldom practical, however, and recourse is usually taken to placing a few well-grounded objects, such as vertical conducting masts or horizontal ground wires, either above or around it, to provide a sufficient degree of protection. Their proper location in order to provide such adequate shielding has been a subject of considerable study in connection with the protection of transmission lines and substations. Standard types of lightning rods as commonly installed on the ridges and elevated points[1] usually provide sufficient shielding for most structures. However, this procedure does not always provide the degree of shielding necessary for hazardous structures. As a result of laboratory studies with models and operating experience,[3,4] the necessary locations of masts and ground wires relative to exposed points have been determined as a function of their efficiency of shielding. In Figures 3 and 4 are given data for the determination of the configuration to provide what is considered to be the best practical shielding. Figure 3*a* applies to the use of a single vertical mast. The necessary height (y) in feet above any point of a structure to be shielded is given as a function of the height (d) of that point above ground and its horizontal distance (x) from the shielding mast. In

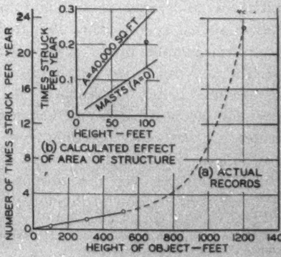


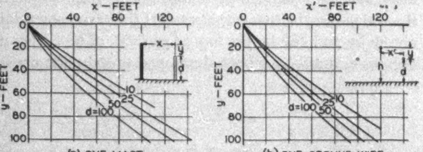
Figure 2. Number of times per year objects of various heights are struck by lightning[3]

Figure 3*b* are similar curves for a single horizontal ground wire. When more than one mast or ground wire are used close enough together that the shielding areas overlap, the curves of Figures 3b and c apply. The region in which various combinations of masts or ground wires will protect objects of a given height is shown in Figure 4.

In the use of these data, configurations of masts or wires can first be chosen so that the higher and apparently more exposed points of the structure lie within the area of shielding as determined from Figure 4. These points will generally be close enough to the same height that one value can be used for *d*. The necessary elevation *y* of the shielding system can be determined from the curves of Figure 3 so that the dimensions *x* and *S* provide an area sufficient to protect all of these points. The adequate protection of lower points that may lie outside of the area so determined can then be checked by determining the larger area of protection for the new height (*d*) and correspondingly greater elevation (*y*) of the shielding system above these points. This will be illustrated in examples given later.

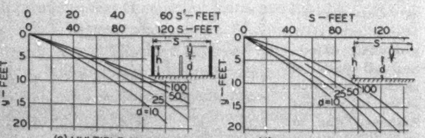
NECESSARY SIZE OF GROUND LEADS

All of the shielding circuit which conducts the lightning current to ground, including the aerial ground wires or the terminating points of vertical masts, the ground leads connecting them to ground, and the driven rods or buried conductor constituting the grounding, need to be of sufficient current-carrying capacity for lightning-stroke currents. In the past[1] it has been customary to specify that, if the ground leads are copper, they should have an equivalent-current carrying capacity of about number 2 American wire gauge conductor which is approximately one-quarter inch in diameter. If galvanized steel is used the equivalent of a three-eighths inch diameter conductor is specified. It is now realized, however, that a number 6 American wire gauge conductor in either copper or steel has more than the necessary current-carrying capacity and sufficient mechanic strength for most applications. Mechanical strength and corrosion are the primary limitations in size. Where overhead wires are used for shielding, however, greater fusion due to arc contact occurs. It is thought that the minimum conductor size which is safe to use for overhead wires is number 2 in either copper or galvanized steel. Stranded conductors are usually best. The terminal points of masts do not need to be so large, since the fusion upon stroke contact would only fuse at the most, a few tenths of an inch of the tip. A number 6 wire, if properly supported, is adequate, and special high-conductivity tips are not required. About three-eighths-inch rods are the smallest that can be driven, and any standard ground rod has adequate current-carrying capacity. Data[5,6] are now available on the necessary thickness of sheet metal in order that holes or appreciable pockets will not be fused by stroke contact. Sheets greater than about three-eighths inch thick cannot be appreciably damaged by the most severe stroke. Thus, metal tanks and the like, which are self-protecting in every other respect, do not require shielding if constructed from metal of at least this thickness.



(a) ONE MAST

(b) ONE GROUND WIRE

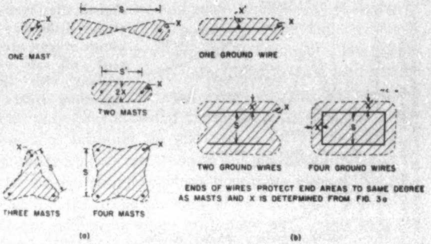


(C) MULTIPLE MASTS

(d) TWO GROUND WIRES

Figure 3. Curves for determining the location of masts and overhead ground wires to shield a structure adequately

Necessary height (y) of shielding objects given as function of height (d) of protected structure and the horizontal distances x and S which determine regions protected



ENDS OF WIRES PROTECT END AREAS TO SAME DEGREE AS MASTS AND X IS DETERMINED FROM FIG. 3a

Figure 4. Regions in which points at a given height (*d*) are protected by various combinations of shielding masts and overhead ground wires

The use of the smaller conductor sizes recommended here results in a considerable savings in materials which are critical at this time. Galvanized steel used instead of copper can represent a considerable saving in the more vital metal.

NECESSARY CLEARANCES

Any region completely enclosed by metal surfaces is shielded not only from contact by direct strokes but also from the fields of any electrical disturbance emanating from without. Potential differences for producing sparking cannot be developed. Metal storage tanks which are completely sealed off would require no form of protection for their contents. Grounding of the tank is only necessary to prevent possible injury or damage on the outside. Sufficient bonding of all surfaces which are not continu-

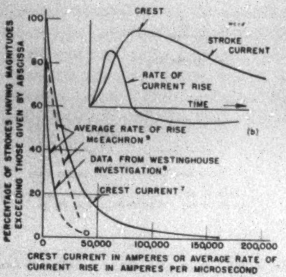


Figure 5. Probability of the magnitude of crest current and rate of current rise in lightning strokes

ous and the proper application of flame arresters at all vents of tanks containing inflammable material usually constitutes sufficient protection.

Little attention need also be given to the contents of buildings whose roofing and siding surfaces are metallic and adequately bonded. However, many cases of hazardous buildings have been encountered where bonding was not provided. In some cases the metal sheeting used was coated with an insulating material so that bonding was very difficult. Unbonded buildings of this type in most cases provide sufficient shielding from electric fields and sparking in their interior, but hazardous sparking is liable to occur between sheets of the material. This is most likely to occur when the shielding system is mounted directly on the structure. Then arcs of high current from the stroke with sufficient energy to constitute a fire hazard can be produced. This hazard is greatly reduced when the shielding system is moved away by separate mounting, as will be discussed later. However, in some cases, low energy sparks can be produced even by indirect strokes occurring in the vicinity of the structure.[7] For any building in which this would constitute an explosion hazard, such construction should not be used unless adequate bonding is provided.

The effectiveness of such interior shielding from electric fields decreases as the amount of metallic surface is decreased. However, practically all buildings constructed of reinforced concrete or structural steel for the roof, floor, and all sides, all bonded together, provide adequate interior shielding. The only necessary requirement is that a continuous metallic path be provided from the stroke terminal point to this metallic grid and then to ground, so that no sparking will be produced that might crack intervening concrete or otherwise cause a sparking hazard.

When only a relatively few conducting paths are provided to ground, it is necessary that their inductive impedance and the grounding resistance be low enough, relative to the clearances from all metallic objects which might provide another stroke path, that sparkover cannot be developed. Such sparking potentials are determined by the nature of the stroke current and the impedance offered to it by the grounding system. The character of the wave shape of lightning-stroke currents is shown in Figure 5a; the current usually rises in a period from one to ten microseconds to its crest magnitude and then decays to essentially zero value in a period of from 20 to 100 microseconds. The crest magnitude of the voltage produced across a ground can be considered as being equal to the product of its measured resistance and the crest magnitude of the portion of the stroke current which passes through it, or it can be considered in most cases as being equal to the product of the total stroke current and the parallel resistance of all the grounds connected to the shielding system. The magnitude of the inductive voltage across a conductor connecting a ground can be considered as equal to the product of the inductance of the lead and the average rate of rise of the current to its crest. The rate of current rise reaches its maximum and decays to zero by the time the current is maximum, and for all practical purposes the two voltages can be considered as occurring at separate times. The curves of Figure 5b give what is considered to be the best available data on the probability of the crest magnitude and the average rate of current rise in a stroke. The maximum crest is 160,000 to 200,000 amperes, and the maximum average rate of current rise that has been recorded[8] about 40,000 amperes per microsecond. In designing for the best possible protection these figures should be used.

The self-inductance of a ground lead can, to good accuracy, be considered as about 0.4 microhenry per foot of length. The nonconducting paths over which sparking may occur can usually be in two classifications. The first of these is air which has a minimum flashover voltage of about 180 kv per foot of spacing for voltage surges of the wave shape produced by the potential across the grounds and about 370 kv per foot for those corresponding to the inductive drop. For practically all other surfaces, such as wood, stone, brick, and the like, conservative values for the two corresponding flashover potentials are 80 kv and 150 kv per foot of surface. The curves of Figures 6 and 7 have been developed to show the neces-

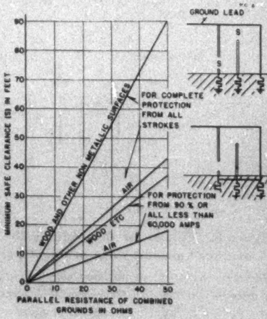


Figure 6. Necessary clearances between separately grounded objects and all parts of shielding system in order to prevent flashover due to resistance drop across shielding-circuit grounds

Upper set of curves recommended for all hazardous structures

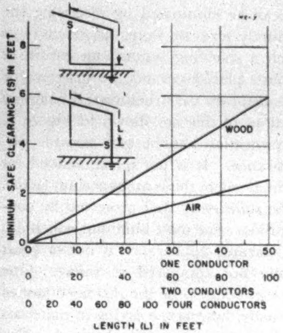


Figure 7. Necessary clearances to prevent flashover due to inductive drop in ground leads

Co-ordinates for abscissa apply to number of ground leads carrying total stroke current to ground

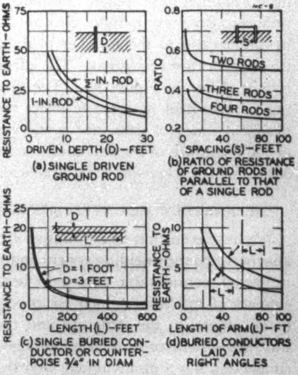
sary clearances which must be maintained to prevent flashover. When the possible flashover gap consists of several in series, the effective length can be considered as the sum of the separate spacings. In Figure 6 is shown the necessary clearances which must be maintained from the shielding system and any objects separately grounded, or the effective clearance necessary to true ground, as a function of the effective combined resistance of the shielding system's grounds.

These necessary spacings are quite large, even for comparatively low ground resistances, such as ten ohms. This illustrates the importance of obtaining low resistance and interconnecting to all grounded objects such as grounded pipes, telephone grounds, and electric power grounds. For cases where metallic objects are not connected to a solid ground but are resting on the ground plane or floor of the building and probably have a high resistance to ground, it is possible in some cases that the potential along the floor of the building will rise appreciably, because of the drop across the lightning ground, so that this full potential difference will not appear between the lightning grounding system and the metallic objects. However, it is not safe to try to take advantage of this, and the minimum safe clearances between a ground or any part of its connecting circuit and such metal objects should be taken as those given in Figure 6. For determining clearances to prevent flashover along the ground, the same curve applying to wood should be used. If the floor is a perfect conducting plane tied into the lightning grounds, the complete potential difference will be eliminated. For cases where lightning rods are used directly on a building and adequate clearance cannot be obtained, such a ground plane may be necessary. This may consist of a bonded reinforced concrete floor. From the standpoint of the potential drop across the grounding system, metal objects in the building do not then have to be grounded. However, the elimination of purely static sparks may require their bonding.

Even after interconnection of grounds or the formation of a good ground plane at the base of the building has been provided, there still exists a possibility of flashover because of inductive drop between grounded objects and the ground leads as illustrated in Figure 7. The rise in potential caused by the inductive drop in a lead can be considerable for the more severe strokes, and sparking can only be eliminated either by increasing the clearances or bridging the gap with a conductor. It is of importance that reduction in this potential difference by the mutual coupling which may exist between the lead carrying the stroke current and a metal object paralleling it can seldom be taken advantage of. Unless they are extremely close, this factor is insignificant.

GROUNDING

It is difficult to specify a general value for the ground resistance which should be provided. In all cases, as shown by Figure 6, the lower the total ground resistance, the better, and all grounds should be made as nearly equal as possible. It is most important to obtain low resistance when interconnection between other grounds and large metallic objects is not provided. Then in practically all cases it should be below about five ohms when the shielding system is mounted on the structure to be protected. When adequate interconnection is provided, the safe upper limit is probably around 20 ohms for the combined parallel ground resistance. When separately mounted shielding systems, such as separate diverter masts or ground wires, are used, the clearances can be made as large as necessary, and a greater flexibility in ground resistance is permissible.



(a) SINGLE DRIVEN GROUND ROD

(b) RATIO OF RESISTANCE OF GROUND RODS IN PARALLEL TO THAT OF A SINGLE ROD

(c) SINGLE BURIED CONDUCTOR OR COUNTERPOISE 3/4" IN DIAM

(d) BURIED CONDUCTORS LAID AT RIGHT ANGLES

Figure 8. Resistance to ground provided by driven ground rods and buried conductors

Calculated from formulas of H. B. Dwight.[10] *Curves apply directly to an average soil resistivity of 100 meter-ohms. For other resistivities multiply resistance by ratio of actual soil resistivity to 100*

In Figure 8 are given curves which show the ground resistances that can be obtained with various combinations of driven ground rods and buried conductors or counterpoise. These curves are based upon an earth resistivity of 100 ohms per cubic meter or 100 meter-ohms. As shown by Table I, this is a general average soil condition. The resistance of a given ground is

Table I. Earth Resistivity for Various Types of Grounding Conditions[11]

	Resistance in Ohms Per Cubic Meter
General average	100
Sea water	0.01–1.0
Swampy ground	10–100
Dry earth	1,000
Pure slate	10^7
Sandstone	10^8

directly proportional to earth resistivity, and the curves can be used for any other resistivity simply by multiplying all resistance values by the ratio of the actual resistivity to that of 100. If the data of Table I are not sufficient to form an idea of the earth resistivity in a given region, and no grounds whose configuration is known are available for measurement, a rod can be driven a given depth and measured. Earth resistivity, of course, varies with moisture content and is lowest when this is highest. However, either a driven rod or a buried conductor has its highest resistance just after it is installed, because its surface contact with the earth is poor until packed down by successive rains. Thus, if the resistance is low enough just after installation, it should be satisfactory at all times.

As shown by Figures 8a and c there is a limit to the length of either a driven rod or buried wire beyond which little is gained in decreased resistance. The use of multiple rods driven in parallel does not lower the resistance in direct ratio to the number of rods, unless their distances apart are very great. The effectiveness of parallel rods is shown in Figure 8b. The diameter of a rod or buried wire does not greatly affect the resistance in the practical range of sizes, nor is the variation very great in the resistance of a given counterpoise for the range of depths which is economically practical.

SEPARATELY MOUNTED SHIELDING SYSTEMS

We have seen that where limited clearances are available between the shielding system and the structure being protected, considerable care must be taken to eliminate chances of sparkover, either by proper arrangement of metal objects in the structure and the members of the shielding system, or by their interconnection. Such interconnection has a disadvantage that stroke current paths are produced which are not specifically designed for this purpose and must be carefully inspected to insure that all portions of them are well bonded and of sufficient current-carrying capacity. The closer the stroke current is to all parts of the structure, the greater the fields which it produces, and the greater the care that must be exercised in the prevention of electrostatic sparking from even small insulated metallic objects. Further, it is frequently difficult to find suitable locations on the structure for mounting the lightning rods in order that adequate shielding be obtained.

These problems can be eliminated by mounting the shielding system entirely separate from the structure to be protected. Such a shielding system can consist of vertical diverter masts placed around the structure or overhead wires stretched over the structure and mounted on masts. Illustrations of this are shown in Figure 9. Such a system of protection has the only possible disadvantage of appearance. It is not recommended for less hazardous buildings where this is an important factor. They can usually be sufficiently well protected by conventional lightning rods. For most buildings which are a serious lightning hazard, appearance is not of great importance, certainly not compared to safety. One limitation is thought by some to be the increased cost of such a system. Actually, however, a saving in materials and labor cost can, in most cases, be obtained. The grounding system does not need to be so elaborate, less conductors to ground are needed, and less attention need be paid to the interconnecting and bonding inside the structure itself. Design becomes basically simpler and requires less detailed attention to assure adequate protection. It has been found that in many cases of ordnance-plant buildings, adequate protection cannot be obtained in any other way without a complete redesign of the buildings. This is particularly true of those mentioned previously, whose roofs and sides are covered with insulated or inadequately bonded sheet metal.

EXAMPLES OF THE PROTECTION OF HAZARDOUS BUILDINGS

The simple structure of Figure 9 is sufficiently typical to illustrate the application of the use of separately mounted shielding systems and to compare them with conventional lightning-rod installations. If lightning rods of the normal length of three to four feet were mounted in the usual manner along the ridge of the roof as shown in Figure 9a, adequate shielding would not be obtained. The edge of the roof is at a height (d) 12 feet above ground and a distance (x) of 20 feet from the rods. As shown by Figure 3a, the necessary elevation (y) of the rods must be 16 feet. Thus, they should be 16 plus 12, or 28 feet above ground, or 8 feet in length to protect the edge of the building. If three such eight-foot rods were mounted at 50-foot intervals on the ridge, they would shield the entire building. As shown by Figures 4a and 3c, two masts will shield an area of a width ($2x$) of 40 feet when S' is 50 feet and y is 16 feet.

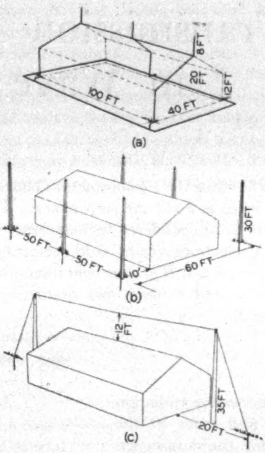
This building can be shielded adequately by the configuration of diverter masts shown in Figure 9b. The necessary distance S between masts on opposite sides of the building is determined by the required clearances from the building. If the floor of the building is reinforced concrete adequately bonded, the mast grounds should be bonded to it, and the mast separation from the building would depend only upon securing clearances for inductive drops in the mast-ground leads. Their closest point on the building should not be over 12 feet from

Figure 9. Examples of lightning protection systems

(*a*). *Lightning rods mounted directly on structure*

(*b*). *Separate diverter masts*

(*c*). *Separate ground wire*



ground, and, as shown in Figure 7, about 0.6 foot is sufficient clearance in air. If the floor is not a good conducting ground plane, the mast ground should be kept a safe distance away as determined by Figure 6. As indicated by this figure, ten feet is sufficient if the resistance for each mast is five ohms. If the resistance is ten ohms, the separation should be 20 feet.

Upon the assumption of ten-foot clearances, the maximum spacing (*S*) to be used in Figure 3c is 60 feet. For this case the most exposed point is along the ridge at height *d* of 20 feet. If six masts are used, two essentially rectangular shielded areas are provided as shown by Figure 4a. For *S* of 60 feet and *d* of 20 feet, Figure 3c indicates the necessary elevation *y* of the masts above the ridge of six feet. Thus the mast heights should be 26 feet.

A single overhead ground wire as illustrated in Figure 9c can also be used. Referring to Figure 3b, to shield the edge of the building for which x' is 20 feet and *d* 12 feet, *y* should be 20 feet, or the minimum height of the ground wire above ground should be 32 feet. This provides a clearance of 12 feet from the building. If a grounded object extended to the roof, this would still provide adequate clearance to prevent flashover due to inductive drop as shown by Figure 7 for two ground leads of 90-foot length, which is approximately the distance from the center of the ground wire to ground. If the shielding grounds are not interconnected to the building, Figure 6 shows that, for 12 feet clearance in air, the parallel resistance of the two grounds should not exceed 12 ohms, requiring about 25 ohms for each.

The masts for Figures 9b and c can be constructed from standard wood poles. A 35-foot pole would provide a height of 30 feet for the diverter masts, and a 40-foot pole, 35 feet for the ground wire, allowing three feet for sag which is more than ample. Number 6 American wire gauge galvanized steel, extending a few inches above the pole top, can be used as ground leads for the diverter masts of Figure 9b. The horizontal ground wire could be number 2 galvanized steel. The necessary guys at the poles can be used as the conductor to ground. It is only necessary that this guy or the ground itself have the clearances specified by Figure 6. The insulated poles can be as close as is consistent with this. For normal soil conditions, driven ground rods are all that is necessary for the separate shielding systems. A buried counterpoise might, however, be necessary for the rod installation of Figure 9a.

With the exception of the extended rods, the system of Figure 9a is similar to what has in the past been commonly specified for hazardous buildings for which at least three-eighths inch copper conductors have been used. Exclusive of the metal used in the interior of the building for bonding, this would require about 250 pounds of copper. The mast system of Figure 9b would require only about 12 pounds of steel conductor and 40 or 50 pounds of ground rods, together with six 35-foot wood poles which, installed, cost only about $30 each. The ground-wire system of Figure 9c requires about 50 pounds of conductor and guys, 12 pounds of ground rods, and two 40-foot poles whose installed cost is about $35 each. The ground wire would provide the least expensive installation for this particular structure. The use of either diverter masts or ground wires permits considerable fluctuation of configuration in order not to interfere with driveways and the like placed around the structure.

Such systems have been used with good results for substations, oil storage reservoirs, and some ordnance buildings. However, their more general use for ordnance buildings which are now being constructed so extensively, would greatly reduce the hazard of such structures and result in a considerable savings in critical materials.

REFERENCES

1. Code for Protection Against Lightning, National Bureau of Standards Handbook *H*21, U. S. Department of Commerce.

2. The Distribution of Thunderstorms in the United States 1904–33, **W. H. Alexander.** *Monthly Weather Review*, volume 63, 1935, page 157.

3. Shielding of Substations, **C. F. Wagner, G. D. McCann, C. M. Lear.** *Electrical Engineering*, volume 61, February 1942, page 96. AIEE *Transactions*, volume 61, 1942, February section, page 96.

4. Shielding of Transmission Lines, **C. F. Wagner, G. D. McCann, G. L. MacLane, Jr.** AIEE *Transactions*, volume 60, 1941, page 313.

5. Lightning Strokes in Field and Laboratory—III, **P. L. Bellaschi.** AIEE *Transactions*, volume 60, 1941, page 1248.

6. Effect of Lightning on Thin Metal Surfaces, **K. B. McEachron, J. H. Hagenguth.** AIEE *Transactions*, volume 61, 1942, August section, pages 559–64.

7. Induced Voltages on Transmission Lines, **C. F. Wagner, G. D. McCann.** AIEE *Transactions*, volume 61, 1942.

8. Field Investigations of Lightning, **C. F. Wagner, G. D. McCann, Edward Beck.** AIEE *Transactions*, volume 60, 1941, pages 1222–30.

9. Lightning to the Empire State Building, **K. B. McEachron.** AIEE *Transactions*, volume 60, 1941, September section, page 885.

10. Calculation of Resistances to Ground, **H. B. Dwight.** *Electrical Engineering*, volume 55, December 1936, page 1319.

11. Symmetrical Components (book), **C. F. Wagner, R. D. Evans.** McGraw-Hill Book Company, Inc., New York, N. Y.

Ultrahigh-Frequency Transmission in Wave Guides

L. A. WARE
ASSOCIATE AIEE

Ultrahigh-frequency, or microwave radiation is being employed more and more in the prosecution of the war, for communication and other specialized purposes. Unlike the power and audio frequencies, transmission in the ultrahigh-frequency region is accomplished best by wave guides, or tubular conductors. In order to make practical use of wave guides, it is necessary to decide upon the shape and size of the guides and upon the mode of transmission to be used. Here transverse-electric and transverse-magnetic modes of transmission in both rectangular and cylindrical wave guides are considered.

GUIDED TRANSMISSION as a term may apply to any system in which electric power is constrained to follow a conductor as in the open-wire, coaxial, or wave-guide line. The name wave guide, however, has come to refer to the system whereby an electromagnetic wave is constrained to move through a tube without a return conductor. In speaking of this method of transmission it is first necessary to review briefly the characteristics of an electromagnetic field in the vicinity of a perfect conductor. In the first place, in such a field the tangential component of the electric field at the surface of the conductor must be zero. Also, in general, the electric field must be perpendicular to the magnetic field at all points.

An approach may be made to the treatment of wave guides by considering an ordinary open-wire line where the transmitted power is subject to changes produced by the line parameters and to loss by radiation. As the frequency is indefinitely increased, radiation and resistance will result in a comparatively high attenuation. The radiation may be reduced by shielding and a coaxial cable results, in which the entire field is constrained to remain within the outer conductor. At very high frequencies the current flows in a very thin layer on the inside of the outer conductor and on the outside of the inner conductor. In the wave guide the field is also entirely contained within the hollow conductor and whatever current exists will flow on the inner surface of the guide. This article treats some practical aspects of the subject but is not concerned with the derivation of the basic equations needed. The derivation of these can be found in many references at present available.[1–4] A knowledge of ordinary line-transmission theory is assumed.

RECTANGULAR GUIDES

The rectangular wave-guide tube has dimensions as shown in Figure 1 where the direction of transmission is to be along the z co-ordinate. The horizontal dimension is taken as b and the vertical as a. The type of transmission to be considered is characterized by the presence of the electromagnetic field components, E_y, H_z, H_x, where E and H are the field intensities in volts per centimeter and amperes per centimeter respectively (Figure 2).

The value of β, the phase constant, for this transmission can be shown to be

$$\beta=\sqrt{(\omega/c)^2-(\pi/b)^2}$$

where b is the width of the tube in centimeters and c is the velocity of light. The lowest frequency which can be transmitted is given by setting $\beta=0$. That is

$$\omega_c/c=\pi/b$$

or

$$f_c=c/2b$$

where f_c is the cutoff frequency. For values of f lower than f_c, β will be imaginary and no transmission can take place. This result leads immediately to the fact that very high frequencies or very large tubes must be used. For instance, if a tube 10 centimeters wide were used, the frequency would have to be at least as high as

$$f_c=3\times10^{10}/20=1.5\times10^9 \text{ cycles per second}$$

Thus wave guides are essentially high-frequency transmission lines and act as high-pass filters.

From the value of the phase constant of the line the following are obtained:

$$v_p=\omega/\beta \qquad \lambda_g=2\pi/\beta$$

where v_p is the phase velocity, and λ_g is the wave length as measured in the guide. The group velocity is given by

$$v_g=d\omega/d\beta=c^2/v_p$$

The above brief outline of the rectangular guide is based upon an exact analysis using the electromagnetic-field equations. In order to see more clearly what the configuration of the field may be within the guide, it is necessary to treat the problem from the electromagnetic standpoint. Let it be assumed that the electric field component is everywhere vertical as mentioned above, that is, in the direction of the y axis. From elementary

---

L. A. Ware, associate professor of electrical engineering at the University of Iowa, Iowa City, was associated with the research department of the Stromberg-Carlson Telephone Manufacturing Company, Rochester, N. Y., when this article was prepared.

considerations, it is seen that this electric field must be zero at $x=0$ and at $x=b$ on account of the perfectly conducting sides. It is shown theoretically that there is a sinusoidal variation of E from $x=0$ to $x=b$. The fact that the magnetic field must be everywhere perpendicular to the electric field leads to the fact that the magnetic lines must lie in planes parallel to the x–z plane.

It is clear that there may exist both H_x and H_z components, but no H_y component. Since the transmission is to be in the z direction, it is to be expected that H_x and E_y should vary with x in the same manner and have the correct sense to produce propagation in the z direction as given by the evaluation of the Poynting vector. There is no net propagation in the x direction on account of the impenetrability of the walls, thus the field components E_y and H_z must be such that the average value of their product is zero, thus making the x component of the Poynting vector equal to zero. The following set of field components is consistent with the above conditions.

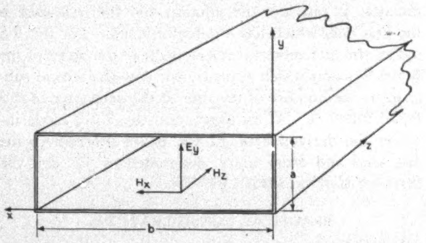


Figure 1. Co-ordinate system for a rectangular guide

$$E_y=A\sin \pi x/b$$

$$H_x=B\sin \pi x/b$$

$$H_z=C\cos \pi x/b$$

In the above expressions the terms involving variations with z and t have been omitted. It is safe to say that z will enter through $\epsilon^{-\gamma z}$ where γ is the propagation constant and t will enter through a sinusoidal term, $\epsilon^{j\omega t}$. If these exponentials are used, it must be remembered, of course, that the final results must be obtained by taking only the real part.

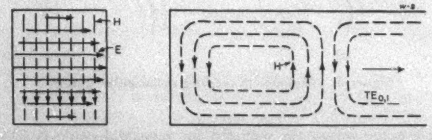


Figure 2. Field configuration for rectangular $TE_{0,1}$ mode

On the basis of Maxwell's equations γ is calculated to be of the form $0+j\beta$. Thus $\alpha=0$ and β has real values only above a frequency f_c. The results of the analysis are:

$$f_c=c/2b$$

$$\beta=\sqrt{(\omega/c)^2-(\pi/b)^2}$$

$$v_p=\frac{c}{\sqrt{1-(f_c/f)^2}}$$

$$v_g=c\sqrt{1-(f_c/f)^2}$$

The mode of transmission outlined above is known as the $TE_{0,1}$. If both E_y and E_x components of the electric field are present, other designations are used such as $TE_{1,1}$, and so forth. These terms are defined in a later paragraph.

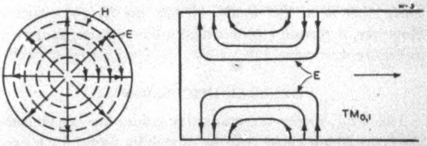


Figure 3. Field configuration for cylindrical $TM_{0,1}$ mode

CYLINDRICAL GUIDES

As for the rectangular guide already considered, the treatment of the cylindrical wave guide is based also on the field equations. The co-ordinate system used is the cylindrical system with r, ϕ, z. The type of transmission known as $TM_{0,1}$ will be briefly outlined below. This mode of transmission is characterized by the existence of only one component of the magnetic field, H_ϕ. This means that the components of the electric field may be E_z and E_r. At the outset the boundary condition demands that $E_z=0$ at the inner surface of the guide. Incidentally, of course, the electric lines E_r must all meet the conducting surface perpendicularly. In a manner similar to that used in the case of the rectangular guide, it can be shown that there must be a certain minimum diameter of tube in order to transmit a given frequency.

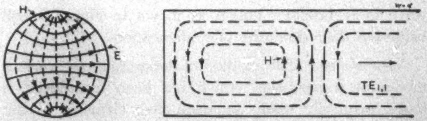


Figure 4. Field configuration for cylindrical $TE_{1,1}$ mode

This limiting point is given by

$$f_c=cp/2\pi b$$

where b is the radius of the tube and p is 2.405, the first root of $J_0(x)$, the Bessel function of the first kind and zero order. The phase constant becomes

$$\beta=\sqrt{(\omega/c)^2-(p/b)^2}$$

The configuration of this field is as shown in Figure 3. Since the $TE_{1,1}$ mode of transmission in cylindrical guides is somewhat similar to the $TE_{0,1}$ mode in rectangular guides, mention of it also is not out of place here. The same basic equations apply to all modes. For instance in this case

$$\beta=\sqrt{(\omega/c)^2-(q/b)^2}$$

where $q=1.84$, the first root of the derivative of $J_1(x)$. Since this is a TE mode, it has no E_z component. However, a certain circular dissymmetry exists as shown in Figure 4.

GENERAL DISCUSSION

The $TE_{0,1}$ mode in rectangular guides has certain advantages which cause it to be especially useful for transmission. In the first place if the tube is made with a dimension *b* somewhat greater than the critical value and *a* somewhat less, the tube can transmit this particular mode only and there is no possibility of the wave shifting its polarization through 90 degrees. No other mode of transmission in rectangular guides at the given frequency has as low a value of *b* at the cutoff frequency. Also this mode has the lowest minimum attenuation of any.

The $TM_{0,1}$ mode has the useful property of being circularly symmetrical and thus it makes no difference if the field rotates as it progresses along the tube.

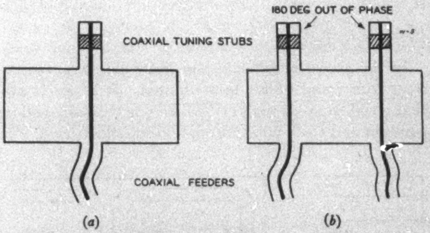
The mode $TE_{1,1}$ in cylindrical guides has no particular advantage as far as this treatment is concerned. A comparison of the $TE_{1,1}$ wave of Figure 4 with the $TE_{0,1}$ wave in rectangular guides as shown in Figure 2 will bring out their similarity of configuration.

Nomenclature. The method of designating the various modes of transmission which has been introduced is based primarily upon the distinctive elements in the configuration of the field. In any mode there will be at least one component, either electric or magnetic, which is parallel to the direction of transmission. It follows then that either the electric or magnetic field existing within the guide will be entirely transverse. For instance, if there exists a longitudinal component of the magnetic field, the electric field will be a transverse one. The nomenclature is based upon the part of the field which is transverse only. Accordingly, if the electric field is transverse with no longitudinal component, it will be designated as a TE (transverse electric) mode. Similarly a TM (transverse magnetic) mode has only transverse magnetic components while it may, of course, have all electric components. In the case of the rectangular wave guides the subscripts refer to the number of half wave lengths which will fit transversely into the tube at the cutoff frequency. The modes spoken of above as $TE_{0,1}$ and $TE_{0,2}$ thus have no variation in the field along the one dimension here taken as the vertical, but will have a variation in the field of one or two half wave lengths respectively along the horizontal dimension. For cylindrical guides, the discussion must be divided into two parts, one devoted to TE waves and the other to TM waves. The letters TE and TM have the same significance as before. For the TM modes, the first subscript refers to the order of the Bessel function involved, and the second subscript refers to the number of the root. For instance in the mode $TM_{0,1}$ the Bessel function J_0 provides the solution and the root used is the first one, which has a value of 2.405. For the TE modes the first subscript refers again to the order of the Bessel function which appears, but now the second subscript is the number of the root of the derivative of this Bessel function. As an illustration the $TE_{1,1}$ mode involves the derivative of J_1, the Bessel function of the first kind and order unity, designated as J_1', and the first root of J_1' which is 1.84.

PRACTICAL CONSIDERATIONS

In making practical use of wave guides, it is necessary to decide upon three things: (1) shape of guide, (2) size of guide, and (3) mode of transmission to be used. It is well to note again the two primary modes of vibration. The simplest one in rectangular tubes is the $TE_{0,1}$ mode which has a configuration as shown in Figure 2. A useful mode in cylindrical guides is the one designated by $TM_{0,1}$ and shown in Figure 3.

Methods of Excitation. The two modes already mentioned are set up in wave guides in an especially simple manner. Since the rectangular guide has electric lines which are shown perpendicular to the longer dimension, the method shown in Figure 5*a* is immediately suggested. Here the transverse wire fed by a coaxial cable is set parallel to the electric field. In order to make adjustment for the purpose of maximizing the transfer of power, a coaxial tuning stub is added.



(*a*) (*b*)

Figure 5. Methods of feeding rectangular guides

In order to set up the $TE_{0,2}$ mode, the arrangement shown in Figure 5*b* is used. In view of these two examples, methods of exciting any mode $TE_{0,n}$ are evident. On account of the similarity in configuration between the rectangular $TE_{0,1}$ and the cylindrical $TE_{1,1}$ modes, a method of exciting the latter becomes evident also. However, the $TM_{0,1}$ mode is of more importance and the method used in its excitation is as shown in Figure 6. Here a length *a* of the inner conductor of a coaxial pair projects into the end of the wave guide and sets up electric flux lines as shown. The effectiveness of the power transfer is dependent upon the length *a* which must be adjusted for any particular guide size and frequency.

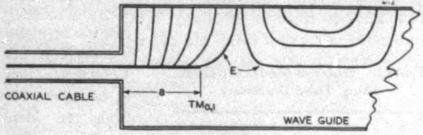


Figure 6. Method of feeding $TM_{0,1}$ mode in a cylindrical guide

Frequency Limits. Transmission will not occur unless the type of wave excited by the particular feeding method conforms to the cutoff-frequency requirements of the wave guide. As an illustration, assume that transmission through a tube having a circumference of 3π inches is required. The cutoff frequencies for various modes are as follows:

(*a*). $TE_{1,1}$ (cylindrical) 2,310 megacycles per second.

(*b*). $TM_{0,1}$ (cylindrical) 3,020 megacycles per second.

(*c*). $TE_{0,1}$ (square) 3,070 megacycles per second.

It is noted from the above that transmission of the $TM_{0,1}$ mode through a cylindrical tube requires a size more than sufficiently large for the $TE_{1,1}$ mode. This means that accidentally both modes may be present while transmitting the $TM_{0,1}$, which is a definite disadvantage in that calculations cannot easily be made if both waves are present. However, to offset this disadvantage, it is to be noted that the $TM_{0,1}$ mode is circularly symmetrical and for that reason detecting equipment placed at any point on the circumference of the guide will pick up signals equally well. The disadvantage noted previously, however, causes the use of this mode to be inadvisable except where necessitated by other considerations.

Turning now to the square guide, it is seen that its cutoff frequency is essentially the same as for the $TM_{0,1}$ mode. However, in a square guide, the cutoff frequency is determined only by the dimension perpendicular to the electric lines, and the other dimension may be made smaller without ill effects except through its influence on the characteristic impedance and attenuation. Also a square guide, such as that previously shown, may transmit the given mode in two positions, that is, due to some irregularity, the same mode with electric lines at 90 degrees to the original position may be set up. This is a distinct disadvantage because detecting equipment is usually connected to pick up only one polarization. The answer to this difficulty is to make the horizontal dimension, or dimension perpendicular to *E*, slightly greater than the critical value for the frequency desired, and the vertical dimension slightly less than this value. By this method then the $TE_{0,1}$ mode in one position only can be transmitted. Also since all other rectangular modes at this particular frequency require larger tubes, this is the only mode that can be transmitted.

Selection of Tube Size. As indicated, tube sizes should be somewhat greater than the critical or cutoff dimension, and in the case of the rectangular guide, one dimension should be greater and the other less. For the cylindrical guide, the curves of Figure 7 are informative.

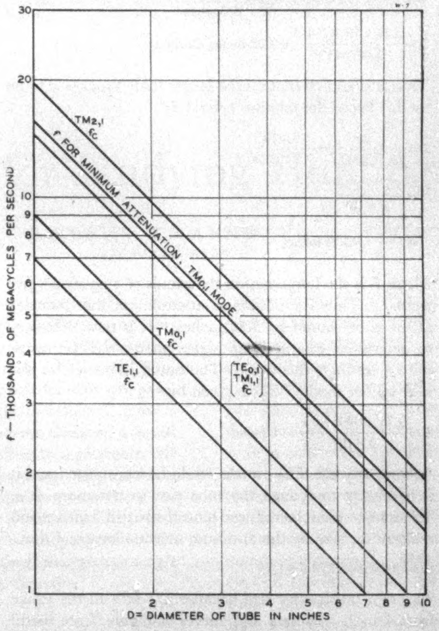


Figure 7. Cutoff frequencies for various modes of transmission

In order to decide definitely on a tube size, it is necessary to consider the effect of size on attenuation. For most modes, a minimum attenuation exists at some definite frequency, and this frequency varies for different tube sizes. It is advisable to select a tube size which will give a minimum attenuation at approximately the frequency to be used. The $TE_{1,1}$ wave has a lower attenuation than the $TM_{0,1}$. However, for short lengths, this disadvantage is outweighed by the advantage, mentioned above, gained by the circular symmetry of the $TM_{0,1}$ mode. The relation for minimum attenuation in the $TM_{0,1}$ mode is $\lambda/b=1.5$. Actually, however, any value from 1 to 2 may be used as in this region the attenuation remains almost constant. For the $TM_{0,1}$ wave, the attenuation is almost flat over a 2,000-megacycle band.

As an illustrative problem, assume that it is required to determine a tube size to transmit 3,000 megacycles per second and to prevent the transmission of all modes above $TM_{0,1}$. The cutoff size is given by:

$$f_c=3{,}000\times10^6=3.76\times10^9\times\frac{2.405}{D\text{ inches}}$$

$$D=\frac{3.76\times10^9\times2.405}{3{,}000\times10^6}=3.02\text{ inches diameter}$$

Thus it is known that a tube larger than 3 inches is to be used. From the relation $\lambda/b=1.5$:

$$D=\frac{2c}{1.5(2.54)f}=\frac{6\times10^{10}}{(1.5)(2.54)f}$$

$$=\frac{15.75\times10^9}{F\times10^9}$$

$$=\frac{15.75}{3}=5.25\text{ inches}$$

where F is the frequency in thousands of megacycles per second. Thus for minimum attenuation, the diameter of the tube should be 5.25 inches. It is now necessary to determine whether any higher modes will be transmitted through this tube. The cutoff diameter for the next higher mode $TE_{0,1}$ is given by:

$$D=\frac{3.76\times10^9\times3.83}{3\times10^9}=4.8\text{ inches}$$

Accordingly the $TE_{0,1}$ mode would be transmitted and it is necessary to reduce the tube size to not more than 4.8 inches. Let it be reduced tentatively to 4.5 inches and recheck the size by the optimum attenuation condition.

$$\lambda/b=10/2.54\times2/4.5=1.75$$

This is a satisfactory size because λ/b falls in the range from 1 to 2. Table I and curves of Figure 7 are useful for calculations such as these.

Selection of Rectangular-Tube Dimensions. For transmission through rectangular tubes, it has already been pointed out that a form other than square should be used in order to maintain what may be called the original polarization. There is also a requirement on dimensions fixed by attenuation. It can be shown that, for various tubes of the same circumference but various values of a/b, a/b should be about 1.18 where a is the dimension parallel to the electric field. It will immediately be noted that these dimensions will allow the same frequency to be transmitted with the other polarization and thus the attenuation requirement cannot be used. Thus, in spite of an increase in attenuation a/b is made less than unity and may be made around one half in order to transmit as low a frequency as possible with as little copper as possible. The detailed considerations for size are somewhat involved, and in practical cases, since the transmission is generally for short distances, the primary determining factor may be convenience.

As an illustration, the attenuation for a copper tube 3 inches by 1.5 inches at 3×10^9 cycles per second is calculated to be about 5.0 decibels per 1,000 feet. This is, of course, far from the ideal conditions. The attenuation calculated for a tube with the optimum ratio of 1.18 with the same cutoff and transmitted frequencies is about 3.0 decibels per 1,000 feet. Note that here no attempt has been made to transmit the optimum frequency. That the attenuation can be very low for ideal conditions is seen from the fact that a tube of 40-centimeter circumference and the optimum a/b ratio at 4,000 megacycles per second is about 1.6 decibels per 1,000 feet.

Table I. Order of Modes for Calculating Tube Diameters

P	Mode (Cylindrical)
1.84	$TE_{1,1}$
2.405	$TM_{0,1}$
3.83	$TE_{0,1}$
3.83	$TM_{1,1}$
5.14	$TM_{2,1}$
5.33	$TE_{1,2}$
5.52	$TM_{0,}$

$f_c=3.76\times10^9\,p/D$ inches

D is the diameter of the tube in inches.

The tube considered above which has an attenuation of 5.0 decibels per 1,000 feet is designed to transmit 3,000 megacycles per second. It is interesting before leaving this part of the general discussion to determine how well this tube meets the cutoff requirement. The cutoff frequency for a rectangular guide is given by:

$$f_c=c/2b$$

$$f_c\text{ (for horizontal dimension)}=\frac{3\times10^{10}}{2(3)2.54}=1{,}970\text{ megacycles/second}$$

Thus certainly 3×10^9 cycles per second is passed easily. For the other dimension

$$f_c=\frac{3\times10^{10}}{2(1.5)2.54}=3.94\times10^9\text{ cycles/second}$$

Consequently this mode will not be transmitted with horizontal polarization. It remains only to show that the next higher mode $TE_{0,2}$ will not be transmitted.

$$\lambda=2b/2=c/f_c$$

$$f_c=\frac{c}{b}=\frac{3\times10^{10}}{3\times(2.54)}=3{,}940\text{ megacycles/second}$$

Since this is above the actual frequency being transmitted, the $TE_{0,2}$ mode will not cause any trouble.

Selection of $TE_{0,1}$ and $TM_{0,1}$ Modes. Sufficient material has been covered to justify the selection of the rectangular guide transmitting its lowest possible mode. For the purpose of ordinary transmission in a fixed system, this method would be satisfactory. However, it suffers from the lack of mechanical flexibility in that it is difficult to transmit the $TE_{0,1}$ mode through elements which may change their orientation with respect to the source. For this reason it would seem that it would be better to transmit only symmetrical waves but as stated above, these permit lower modes which will interfere with measurements. A compromise must be made by using a symmetrical mode in movable sections and changing from one to the other whenever necessary. This requires, of course, an easy way of making the transfer from rectangular to cylindrical guides and vice versa. The measurements, being confined to the rectangular sections and thus, to the one mode of transmission $TE_{0,1}$, will show any irregularity in the cylindrical sections only through an indicated increase in the transition loss.

REFERENCES

1. Electrical Concepts at Extremely High Frequencies, **Simon Ramo.** *Electrical Engineering*, volume 61, September 1942, pages 461–4.

2. Transmission of Electromagnetic Waves in Hollow Tubes of Metal, **W. L. Barrow.** Institute of Radio Engineers *Proceedings*, volume 24, October 1936, pages 1298–1328.

3. Electromagnetic Waves in Hollow Metal Tubes of Rectangular Cross Section, **L. J. Chu, W. L. Barrow,** Institute of Radio Engineers *Proceedings*, volume 26, December 1938, pages 1520–55.

4. Hyperfrequency Wave Guides—Mathematical Theory, **J. R. Carson, S. P. Mead, S. A. Schelkunoff.** *Bell System Technical Journal*, volume 15, April 1936, pages 310–33.

5. Hyperfrequency Wave Guides—General Considerations and Experimental Results, **G. C. Southworth.** *Bell System Technical Journal*, volume 15, April 1936, pages 28–4309.

6. Communication Circuits (book), **L. A. Ware, H. R. Reed.** John Wiley and Sons, New York, N. Y., 1942. Chapters XII, XIII.

An Electronic Circuit for Studying Hunting

MANUEL J. DeLERNO
ENROLLED STUDENT AIEE

ROBERT T. BASNETT
ASSOCIATE AIEE

Various methods have been devised for determining power-angle oscillations of synchronous motors during the period of hunting following a change in load. This article describes an electronic circuit for this purpose, in which a beam of light is reflected from a set of equally spaced mirrors clamped around the motor shaft or coupling to a photoelectric cell; the light flashes reaching the photoelectric cell are amplified and impressed on the plates of a cathode-ray oscilloscope in such a way that the resulting beam pattern on the oscilloscope screen shows the power-angle oscillations directly.

WHEN a load is applied to a synchronous motor, there is a brief period of unstable operation in which the speed fluctuates slightly in order to allow the motor to adjust itself to the new operating condition. This interval of speed variation is known as the period of hunting. The problem of hunting is an important consideration in the design of synchronous machines, for such oscillations may often cause undesirable voltage and power fluctuations or the falling out of step of synchronous motors.

Although several methods have been developed for determining transient power-angle oscillations, they are all subject to certain restrictions and disadvantages. The new method proposed by the authors for the determination of the hunting characteristics of synchronous machinery was designed with the following in mind:

1. It should not involve any auxiliary apparatus that is difficult to obtain or set up.

2. It should be readily adaptable to equipment already in service and not operating under special conditions, such as in a laboratory.

3. The apparatus should be as simple as possible.

Suppose that a mirror is placed on the shaft of a synchronous machine and so arranged as to reflect a beam of light from a lamp into a photoelectric cell every time the rotor of the machine is in a particular position; then while the machine is running at synchronous speed, the photoelectric cell will receive flashes of light at equal time intervals. However, if hunting occurs, the time between successive flashes will vary: As the speed increases, the time decreases; conversely, as the speed decreases, the time increases. If a capacitor is arranged so as to begin charging at a constant rate at the same point on every wave of the potential applied across its terminals, and this capacitor is connected to the photoelectric cell in such a manner that every time the cell receives a flash of light, the capacitor is discharged, then

---

Essential substance of a paper "An Electronic Circuit for Determining Power-Angle Oscillations," which was awarded the AIEE national prize for Branch paper for the academic year July 1, 1940 to June 30, 1941. The paper was presented at the AIEE Southern District student convention, Tuscaloosa, Ala., April 4, 1941.

Manuel J. DeLerno and **Robert T. Basnett** were both students at The Tulane University of Louisiana, New Orleans, when the paper was presented; Mr. Basnett is now control engineer with the Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa. Mr. DeLerno is now development engineer in the industrial control department of the General Electric Company, Schenectady, N. Y.

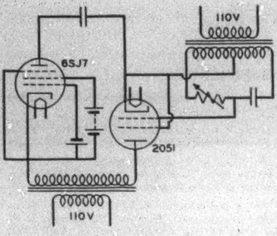


Figure 1. Schematic diagram of circuit for charging a capacitor at a constant rate

the charge on the capacitor varies as the length of time between flashes varies. For a given capacitance, the voltage is proportional to the final charge. Hence, the maximum voltage would follow the same fluctuations as the speed; that is, this voltage variation would be proportional to the hunting.

For the purpose of analysis the device that was evolved is considered as being made up of four distinct electrical and two mechanical units. The two mechanical aids consist of a lamp for producing a focused beam of light, and a metal band to which are attached several small mirrors. These mirrors are built into frames that are not rigidly fixed to the band, so that adjustments can be made before they are set permanently in position. The band itself is arranged so that it can be slipped onto the coupling of the machine under test and clamped. Because of the nature of the electrical components of the device, it is essential that there be as many mirrors as the machine has pairs of poles and that these mirrors be spaced approximately equidistant around the periphery of the coupling.

On the four-pole machine tested with the original apparatus, two such mirrors, about $^{3}/_{8}$ by $^{1}/_{2}$ inch in size, fastened to a 6-inch-diameter $^{5}/_{8}$-inch-wide 20-gauge brass band, gave very satisfactory results. Details relating to the construction of this unit and its alignment are discussed later.

The fundamental unit of the electrical system is a circuit for charging a capacitor at a constant rate. This is accomplished by connecting a vacuum pentode (*6SJ7*) in series with a gaseous tetrode (2051) and a capacitor. The circuit diagram for this part of the setup is shown in Figure 1. From the characteristic curves of this pentode, it can be seen that once the plate voltage has risen above a certain minimum value, the current through the tube is nearly independent of the plate voltage, and is, consequently, essentially constant in magnitude. To prevent conduction from occurring before the applied voltage is above this necessary minimum is the duty of the 2051 tube. By supplying this tube with a sufficiently negative grid voltage, conduction can be postponed the desired length of time, after which the circuit acts as if this gaseous tube were not present, for once breakdown has taken place the current is governed by the pentode only, until the applied voltage has dropped to such a value that it is equal to the capacitor voltage at which time conduction ceases. For the purpose of adjustment the grid voltage of this gaseous tube is provided by a phase shifter, which allows control over the time at which the tube breaks down and the capacitor charging current starts flowing.

Connected across the terminals of the capacitor in this charging circuit is a second gaseous tube (2051) the grid potential of which is governed by the second electrical unit of the apparatus—a gaseous photoelectric cell and its amplifier. This is illustrated in Figure 2. The photoelectric cell receives flashes of light from the mirrors fastened on the shaft of the synchronous machine, each flash being transformed into an electrical impulse which is transmitted through a single-stage resistance-capacitance-coupled amplifier to the grid of tube 2051. This grid has a bias of -20 volts, and, when it receives the sharp, positive pulse of about 90 volts from the amplifier, it becomes immediately conductive, and discharges the capacitor bringing the voltage of the capacitor to zero in a very short time. This statement assumes that the phase relation between the photoelectric-cell pulse and the capacitor voltage is such that the gaseous-discharge tube is made free to conduct at some particular instant after the capacitor voltage has begun to rise; this is the normal and desired operating condition. Since the charging circuit is rectifying in nature, it is obviously useless to have the discharge occur during the half cycle when no charging current is flowing, and consequently no voltage across the capacitor.

The third electrical unit required is a power supply. This needs no description for it is built along standard lines, being made of a type 80 tube and a filter section.

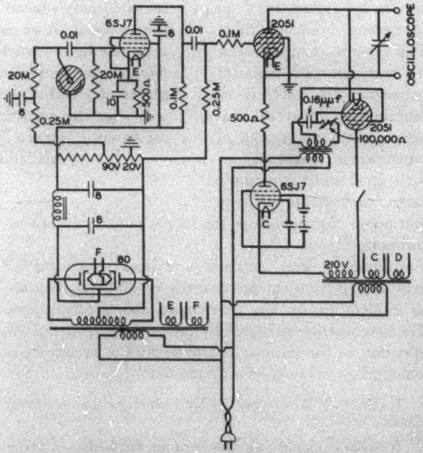


Figure 2. Schematic diagram of the gaseous photoelectric cell, its amplifier, and the charging circuit

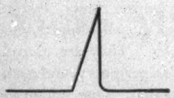


Figure 3. Form of wave recorded on the oscilloscope screen

The output is 300 volts and is used to supply the negative grid bias for the discharge tube and the plate voltages of the photoelectric cell and the *6SJ7* tube in its amplifier. The three units mentioned thus far are built on one chassis, and constitute the special circuit required by this method. The fourth unit is a cathode-ray oscilloscope or a magnetic oscillograph, which are usually available as individual pieces of apparatus and need not be constructed integrally with the circuit. Either instrument may be used in this work, although the oscillograph requires a special coupling circuit. However, the oscillograph has the advantage of being able to show several quantities on one film. The oscilloscope is somewhat easier to use, although it requires that a photographic record be taken, for the hunting wave obtained is not a periodically repeating wave such as a wave of current or voltage in a stable circuit. The results described herein were obtained with a cathode-ray oscilloscope and the remaining discussion relates to its use, but it should be remembered that the magnetic oscillograph would serve equally as well.

The vertically deflecting plates of the oscilloscope are connected to the terminals of the capacitor in the charging circuit. If then the proper sweep frequency is chosen, and the synchronous machine is operated at constant load so that no hunting occurs, a triangular wave appears on the screen of the instrument. Figure 3 shows the form of this wave. How it originates is as follows: Current begins to flow in the charging circuit at some instant regulated by the phase shifter. Because this current is constant in magnitude, the charge across the capacitor increases at a constant rate, and the capacitor voltage rises accordingly. Before the capacitor has completed its charge, however, it is discharged by the photoelectric cell, and its potential drops to zero until the next cycle. When the load on the machine is changed, and equilibrium has been re-established, the length of time that elapses between the beginning of the charge and the discharge pulse is likewise changed, due to the shift made by the rotor. This causes an increase or decrease in the height of the triangular wave that is proportional to the change in power angle of the machine. Strictly speaking, the variation in altitude is proportional to the shift in phase between the rotor and the applied voltage, but this is close enough to be considered as equal to the shift in power angle.

During the period of instability or hunting, the amplitude of the triangular wave is constantly varying. This variation can be seen on the screen, but is of little value, since one deflection falls right on top of the preceding one and the eye is not quick enough to follow the cycle of change. To make a record of the hunting cycle, a very slow, nonrepeating sweep must be used on the oscilloscope. The internal sweep circuit of the average instrument is not suitable for this purpose, and an external sweep must be used. The voltage across a capacitor that is charged through a resistance from a constant-potential source gives a fairly linear horizontal sweep for the interval of time involved, but a more elaborate and accurate one can be constructed in the same manner as the internal sweep. If this sweep is started at the time the load on the machine is changed, the triangular waves will be traced in sequence, one after another, by a moving spot. By making a time exposure with a camera—keeping the shutter open until the sweep is completed—a complete set of waves is obtained, the tips of which form the locus of the hunting wave. The oscillogram reproduced in Figure 4 was taken using a sweep circuit consisting of a four-microfarad capacitor and a one-megohm resistor in series with a 45-volt battery. In obtaining this oscillogram, the switch used to load the synchronous motor also closed the sweep circuit, ensuring proper co-ordination. One feature of these results is that no timing wave of any kind need be obtained, for the start of any one triangular wave is $^1/_{60}$ of a second behind the corresponding origin of the preceding one, providing that the voltage to the circuit remains constant. This supplies a convenient time axis.

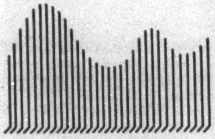


Figure 4. Reproduction of a typical power-angle oscillogram taken on a four-pole synchronous motor

Because the operation of this apparatus involves time, the sequence in which different parts of the circuit function is a prime consideration. For instance, if the pulse from the photoelectric cell is received after the charging current has ceased flowing, the voltage wave projected on the oscilloscope screen has a flat, horizontal top, because there is a maximum voltage to which the capacitor can charge, depending on its capacitance, the charging current, and the applied potential. This flat portion may be eliminated by delaying the start of the charging by means of the phase shifter in the first gaseous tube.

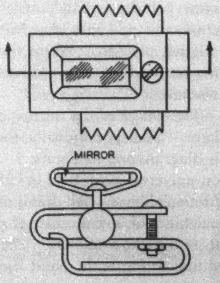


Figure 5. Diagram of mirror attachment

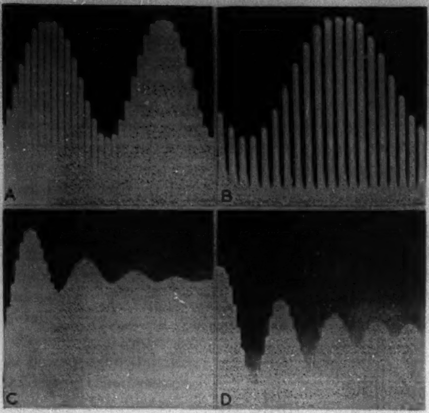


Figure 6. Typical oscillograms taken by the method described in this article

A. Double-current generator operating as synchronous converter (no amortisseur winding)

B. Same as A except with different oscillographic sweep

C. Full load thrown on a synchronous motor

D. Full load removed from a synchronous motor

However, if the flat portion is nearly a half cycle in length, the terminals of the 110-volt lead from the supply to the equipment should be reversed, for this trouble means that the discharge tube is receiving its photoelectric-cell pulse 180 degrees too late. Correction by use of the phase shifter cannot always be applied. If charging is delayed too long, the maximum voltage obtainable, and consequently the deflection on the fluorescent screen, is considerably reduced, for the capacitor voltage becomes equal to the applied potential at some point long after that potential has passed its maximum. If such is the case, all of the mirrors must be shifted an equal amount in the direction of rotation. One other trouble arises when the machine is reconnected after a period of idleness. If the d-c field is not connected with the same polarity it had during the previous test, the machine will pull into step 180 electrical degrees from its original position. When this happens, the field should be reversed or the mirrors shifted a corresponding amount.

The design of the mirror attachments is an important item. The type built for the apparatus described here is diagrammed in Figure 5. The mirrors are mounted on universal joints held in closed S frames equipped with friction clamps. All that is necessary to test synchronous machines of any number of poles is to have a supply of these mirrors available. The only thing that needs individual fitting is the band itself which must be cut so as to encircle the particular coupling on the rotor. Before attaching the band, as many mirrors must be slipped onto it as the machine has pairs of poles. They are placed approximately equidistant around its periphery, and each is held in place with a spot of solder. One mirror is chosen as reference (this mirror need not have a universal joint). This reference mirror is placed in any convenient position; then the photoelectric cell is arranged so as to catch the reflected beam of light from the lamp. The position of the rotor should be observed and marked. Next, the rotor is revolved 360 electrical degrees, so that the 360-degree mark coincides with the fixed reference. By adjusting the second mirror on its universal joint, it can be made to catch a beam focused on it by the lamp and reflect it into the photoelectric cell. It is then clamped with the adjusting screw. The process is repeated for each of the remaining mirrors. This system, therefore, does not require that the mirrors be spaced exactly right, but that the photoelectric cell receive a beam of light 360 electrical degrees after the preceding flash. With reasonable care and a sharp focus, this can be accomplished. If the flashes are not 360 electrical degrees apart, triangular waves having equal altitudes will not be obtained even when the machine is not hunting, and the results will be useless.

This circuit seems to be an improvement in several respects over other devices for determining power-angle oscillations. These may be summarized as follows:

1. The method is applicable to synchronous motors having any number of poles, without additional equipment.
2. The normal operating characteristics are not influenced to an appreciable extent by the apparatus.
3. The end of the motor shaft need not be available. The only room necessary is enough to mount the mirrors on the shaft.
4. This apparatus is equally suitable to measure constant power angles.

Westinghouse photo

Exciter end of a 15,000-kva synchronous condenser

More Mica From the Western Hemisphere

Mica, which heads the list of critical raw materials, is needed so urgently that quantities are being flown to the United States from India. Action is being taken now toward developing domestic mica resources, chiefly in North Carolina, Georgia, New Hampshire, Connecticut, and South Dakota. In this article mica sources, especially those in the Western Hemisphere, are surveyed and the production problem is found to be based on the dearth of skilled, low-cost labor.

WITH war-caused shifts in world trade has come an increasing interest in sources of mica in the Western Hemisphere. Argentina, Brazil, Mexico, Canada, and the United States all have rich deposits of the mineral. These deposits are being surveyed with a view to increasing present production and commerce.

India is by far the largest producer of mica. Out of a total world production of sheet and splittings totaling approximately 9,016 short tons in 1938, India supplied 6,334, Madagascar 747, the United States 469, Canada 68, Argentina 258, and Brazil 575.

In the other American Republics, production of mica for strategic uses has been almost entirely confined to sheet mica. Previously the mineral was exported chiefly to Germany, to India (for splitting) and, in small quantities, to the United States. In recent years, production has increased very considerably both in Brazil and in Argentina. Brazilian exports rose from 49 short tons in 1929 to 575 in 1938 and 1,229 tons in 1940. After the outbreak of war in Europe, there was an increase in exports to Japan, the United States, Great Britain, and India. Argentine exports rose from 58 short tons in 1929 to 248 in 1938.

Thus both Brazil and Argentina have been small but increasing producers of mica. As yet they supply only sheet mica. In the case of sheet mica, the quality of the deposits is the chief factor in determining where to produce; in the case of mica splittings (mica split into thin films, later to be joined together to make large sheets of mica), labor costs are the determining factor. Deposits of mica in Brazil, Argentina, Peru, and Mexico could be developed considerably for crude mica. Likewise, production of amber mica in Canada can be expanded if prices rise. If the Western Hemisphere should become dependent upon its own resources, even with increasing prices and production there would, in all probability, be available a large part of mica requirements.

The problem of production in the Western Hemisphere is chiefly one of finding cheap and efficient labor to grade and split the crude material. Consumers of mica have become accustomed to the excellent grading developed in India from long years of experience. The expansion of sheet production in Brazil and Argentina has been hampered by the fact that standards of classification have not been carefully followed. Consequently, purchasers in this country have preferred to pay higher prices for Indian sheet mica, because they can rely upon deliveries to be exactly as specified. Thus, if sales of sheet are to be improved in Brazil, it is essential that a careful system of grading be established.

Various plans have been suggested for introducing the mica-splitting industry into the Western Hemisphere. Canada already produces a small quantity of splittings and could produce more if prices rose. Small quantities of splittings have been made also in the United States, and in Mexico. Puerto Rico has been suggested as a possible center for mica splittings on account of its abundant supply of cheap labor. Mexico also might increase its splitting industry. Even in these two areas, however, prevailing wages are very much higher than those in India.

HISTORY

Since the time when the first mine was dug in New Hampshire in 1803 to obtain mica for stove windows and lantern chimneys, the uses of mica have become much more varied. With advances in the field of electricity, mica has become an essential material in the manufacture of electric machinery and appliances. The invention of built-up mica in 1892 made possible many of the modern uses for the mineral. Built-up mica is made up of a number of thin films of mica held together by some insulating cement, usually shellac. Such sheets can be made in any size, and then cut down for specific uses. Through the development of built-up mica, it was possible to adopt standard designs for generators and motors. It was also possible to use many small pieces of mica to build up a sheet of much larger size than could be found frequently in natural mica formations.

The property which makes mica indispensable in electrical work is that it can be divided into thin, flexible, transparent films, which are unaffected by fire, water, electricity, or acid, and whose volume remains constant in extreme heat and cold. These characteristics are found in no other substance, and no synthetic substitute having similar qualities has been discovered so far.

MINING

The best quality mica is found in regions which have been relatively free of earthquake and volcanic dis-

---

This is an excerpt from an article, "Strategic Mica," published in the March 14, 1942, issue of *Foreign Commerce Weekly*.

turbances. The most important among these are the Indian Peninsula, Central and South Africa, and the eastern parts of the Western Hemisphere. Geologically, mica occurs in two forms, namely muscovite or white potash mica, and phlogopite, or amber magnesia mica. Mica found in India, the United States, and Brazil is of the muscovite variety, while that mined in Madagascar, Mexico, and Canada is phlogopite.

Mining of mica is made difficult by the fact that mica does not occur in any specific part of the pegmatite dike, the rock formation in which it may be found. The dike itself is usually less than 300 feet in length, comparatively narrow, but of variable depth. Diamond drilling which explores only a small area is not practical, because it might miss the mica entirely. In addition, even when mica is found, it may prove to be of inferior quality and suitable only for grinding. Mica mining is usually carried on with little equipment, since it is impossible to determine how extensive a deposit may be in any given area. Furthermore, even when large deposits are found, the return is comparatively small.

Accordingly, mica mining in the United States is usually conducted on the principle that the work must pay for itself as it proceeds, and substantial capital expenditure is rarely made. In many cases, mica mines which have been deserted when a streak of mica has disappeared, have been reopened later by more hopeful prospectors, or on account of a rise in the price of the product. This is true of mines in India and Canada as well as in the United States.

Many of the chief problems of mica production, particularly in the production of strategic types of mica, are concerned with preparation and sorting. When the chief use of mica was for stove windows, large sheets only were usable. Today there are uses for many smaller sizes. In addition, the development of micanite has increased considerably the utility of small blocks of mica.

Only a very small percentage of the total available mica is suitable for block mica or mica splittings. Although the percentage varies in different localities, usually 70 to 85 per cent of the output of a mine is saleable only as scrap. A further percentage is suitable for punch mica, and about 10 per cent for sheet mica. Of the knife-trimmed sheet, 50 to 70 per cent will be the smallest acceptable size, $1^1/_2$ by 2 inches, rarely more than 10 to 13 per cent will be as large as 3 by 4 inches; and it is always possible that a large part of these sheets will be stained, and thus unsuitable for all uses.

CLASSIFICATION

In appraising the mica deposits, the most important fact is that the substance is found in a variety of different sizes and states of purity, each of which is of a different degree of utility. For many strategic uses, mica must be of certain minimum dimensions, and must also be without stains, which are an indication of the presence of another material, such as iron, not having the same physical characteristics as mica.

For commercial purposes, mica is classified in four categories according to size. Scrap or waste mica is so irregular in size or formation that it can be used only for grinding. Punch mica is sheet mica, usually less than $1^1/_2$ by 2 inches in size. Splittings are sheet mica usually less than three quarters of a square inch in area, and 0.001 to 0.0012 inch in thickness. Splittings mainly come from material too small for use as sheet or block mica, which is the next category, usually ranging in size from $1^1/_2$ by 2 inches to 8 by 10 inches in area, and not less than 0.01 inch in thickness. In the United States, mica sheets are classified according to the size of the largest standard rectangle that can be cut from the sheet.

From the standpoint of strategic importance, only mica sheets or blocks and splittings are regarded as of value, and thus, in considering mica production in relation to war production, the only figures of value are those which distinguish the output of the different types of mica. Not only is mica graded by size, but also by quality, distinction being drawn between clear, slightly stained or spotted, and heavily stained or spotted mica.

Scrap Mica. Waste and scrap mica, which are used for roofing paper, are of value only in the United States, because only there has the process of grinding become adequately developed. A considerable amount of waste is imported for grinding, and some of the ground product is exported.

Block or Sheet Mica. In production of block mica, the preparation and grading are of the greatest importance. Because of the presence of highly trained labor and low wage rates, India has become the chief source both for block mica and for mica splittings. To prepare mica as sheet material, the rough pieces are split into thicknesses from 0.01 to 0.10 of an inch thick, and are then trimmed of imperfections. The sheets are then graded according to the largest rectangle of specific dimensions which can be cut from them. Sheets thus graded as to size are sorted then according to quality. Men who do this work are highly paid, as they can often eliminate imperfections by resplitting a sheet, thus considerably improving the quality and value of the mica.

Splittings. The most important form in which mica is used is as mica splittings. As stated above, India is by far the most important source of splittings, because of favorable labor conditions. There, many small "books" (the term for mica crystals, somewhat like books in shape) which in other areas would be discarded, are used for making splittings. Many such "books" have been recovered in India from waste dumps which accumulated through the centuries before the discovery of the manufacture of micanite, when the demand was exclusively for large sheets of mica. Mica may be split to a thickness of one one-thousandth of an inch. The work is done largely in homes by women and children working at very low wages—from 9 to 12 cents a day. Amber mica, like white mica, is split by hand. For many years Canada produced a considerable quantity of amber mica splittings, but when the amber mica deposits of Madagascar were developed, the cheap labor available there enabled producers to market amber mica at prices with which Canadian producers were unable to compete.

A number of attempts have been made to produce a satisfactory machine for splitting mica. One for splitting amber mica was relatively successful, and was used commercially, but was finally abandoned because of its limitations in producing splittings of required thickness. Many mica experts are of the opinion that there never will be a really satisfactory machine for splitting mica, particularly for muscovite, the splitting of which is more difficult than that of the amber variety.

Fortunately, large supplies of mica are found in the Western Hemisphere. The United States and Canada have for some years been sources for mica. The crucial problem in production, should the hemisphere be separated from Indian sources, lies not so much in lack of the raw material as in the lack of trained, cheap labor to grade and split the material.

Switchgear Practice in Europe and America

J. B. MacNEILL
MEMBER AIEE

America and Europe are developing different types of switchgear equipment, conditioned by the type of facilities available in certain regions and the particular requirements in those regions. Present trends in switchgear apparatus are guided to a considerable extent by the abundance or scarcity of the material resources involved in construction and operation of the equipment.

DURING the past several years, there has been a major change in the handling of switchgear problems—the strong tendency toward completely factory-assembled and tested equipment. American switchgear was slow to adopt this type, which got its start in Great Britain. The present American practice, however, justifies itself by providing at a reasonable cost the adequate interrupting capacity, ease of inspection and maintenance, and freedom from trouble which are essential to our use.

Since metal-clad assemblies in America have been more recently developed than in England, they have incorporated improvements in circuit-breaker design which have resulted in simplification of the complete equipments without loss of safety. It has not been necessary to the same extent to provide for defective switch operation.

Because there have been in the past ten years major differences between our circuit-breaker construction and those of continental Europe, the subject has had a great deal of discussion. At times we have seemed backward in adopting new forms of interrupter having a minimum of oil or no oil at all, which our European cousins have told us were adequate for their service. However, there are differences between our situation and that in Europe.

1. Our rupturing capacities both at high and low voltage are greater than those generally used in Europe.

2. Reclosing duty cycles have been standard with us for 15 years and have needed more adequate interrupting devices with better assurances of trouble-proof operation and reduced maintenance.

We have sold in the past few years power-station circuit breakers for 2,500,000-kva capacity at 13 kv. The same capacity is frequently sold at 132 kv and 230 kv, and there have been discussions of 3,500,000 kva at the higher voltages. Rugged mechanical devices typical of American switchgear are necessary for these extreme requirements. Nevertheless, for approximately 15 years in America there has been a continuous search on the part of several manufacturers for new interrupting ideas that might be incorporated in switchgear when these should appear feasible. The Westinghouse company developed the "De-ion" breaker experimentally for voltages up to 15 kv in the years 1925 to 1927. It was tested on the Commonwealth Edison System in Chicago to over 500,000 kva at 12 kv in 1928, and a large number were installed on the same system in 1930–31. Later experimental developments

---

Essential substance of an address before the AIEE Toronto Section, March 13, 1942.

J. B. MacNeill is manager of engineering, switchgear division, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

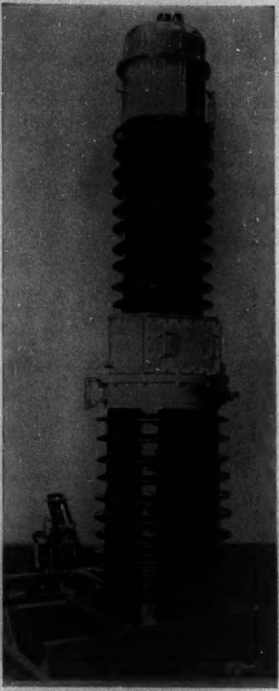


Figure 1 (left). Single-pole 138-kv 1,500,000-kva vertical-flow oil-poor circuit breaker

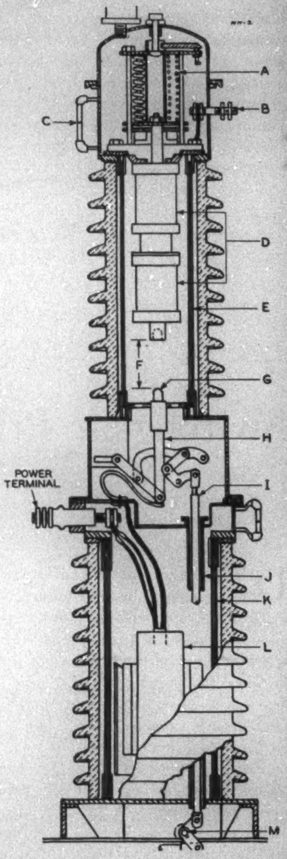


Figure 2 (right). Schematic diagram of the single-pole breaker

A—Opening accelerating springs
B—Power terminal
C—Oil level
D—Interrupters
E—Insulating supporting tube
F—Oil disconnect gap between terminals—breaker open
G—Disconnect member
H—Pole-unit mechanism
I—Insulating operating rod for both opening and closing
J—Insulating tube surrounding operating rod
K—Insulating supporting tube
L—Oil-insulated current transformer
M—Torsion shaft

have covered vacuum breakers, boric-acid breakers, oil-poor breakers and compressed-air breakers.

To get a better view of American switchgear requirements, the field is divided into three classes:

1. Power-station auxiliary and small substation work 15 kv and below, 250,000-kva interrupting capacity and below (generally indoor).
2. Power-station and large substation main switchgear 23,000 volts and below, interrupting capacities up to 2,500,000 kva.
3. Outdoor high-voltage requirements 23 kv to 287 kv rupturing capacities up to 2,500,000 kva.

A proper understanding of the present situation and the changes that have taken place will be reached if we consider each class by itself. These class lines, of course, cannot be considered definite, because the classes mix to some extent, but for our purpose they are adequate.

POWER-STATION AUXILIARY AND SMALL SUBSTATION EQUIPMENT

Present American Practice. While in the early days of electrical engineering various air-type devices were used in the field of power-station auxiliary and small substation equipment, oil circuit breakers have since superseded them for a-c work. Numerous oil breaker designs both oiltight and nonoiltight are available in common tank and multiple single-tank constructions. The maximum savings in space and cost are generally found by the use of common tank devices. While the single-pole form was preferred years ago because of phase isolation, the improvements in design have made the more compact structure quite possible up to 250,000 and even 500,000 kva. The recognition of this type for high-power work resulted from the Detroit tests of 1924 in which the main 4,800-volt bus of the Delray station was short-circuited through a three-pole breaker having a 20-inch tank. With this type it was possible to modernize many outgrown equipments within existing space and with freedom from physical demonstration and oil throwing.

European Developments. In Europe, particularly Germany and France, several devices have been developed in an attempt to supplant the oil breaker. Of course carbon breakers and contactors fill the 600-volt field most frequently there as in America. However, above 600 volts compressed air, self-generated air blasts, chemical resins, and magnetic types are being used.

Recent American Developments. In the United States, practically all commercial developments of the De-ion type of gear have been for self-contained units incorporating magnetic blowout effects in one form or another. One large manufacturer features Magne-Blast in which a powerful magnetic field drives an arc between converging barriers so that the power factor of the arc is increased before rupture. A second manufacturer sells a magnetic type up to 5,000 volts featuring double blowout coils inserted by the arc action as it moves off the main contacts, and these coils lengthen and cool the arc until it becomes unstable. The "De-ion" breaker development is available throughout this class but has a current-interrupting limitation which prevents its use in the largest capacities. A modified form of "De-ion" breaker called "*DH*," at 2,500 and 5,000 volts, provides interrupting with a short total arc length and covers the field up to 250,000 kva at five kilovolts, with prospects of going higher.

POWER-STATION AND LARGE SUBSTATION MAIN SWITCHGEAR

Present American Practice. Since approximately 1895 the oil circuit breaker has practically without exception handled power-station and large substation switchgear in America. The rupturing capacity requirements are great, 1,500,000 kva being a frequent requirement and 2,500,000 kva occasionally called for. The dead-tank circuit breaker is made by all manufacturers and the live tank form by one. In the early years of the development concrete and brick cell work was generally used to limit the effects of distress operations on the breakers. More recently, however, the integrity of the breaker itself has improved so that it has been possible to confine oil and gas to the interior of the structure with proper venting arrangements for hydrogen fumes and with complete absence of flame external to the breaker. Tests in high-power laboratories and further tests on large operating systems have made this development possible.

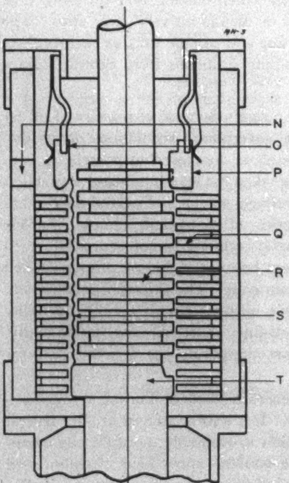


Figure 3. Schematic diagram of the upper unit of the vertical-flow interrupting element of the single-pole breaker

N—Vent
O—Stationary arcing contact
P—Stationary main contact
Q—Stationary corrugated wall
R—Moving corrugated follower
S—Arc
T—Moving contact

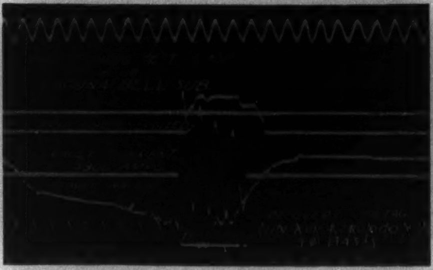


Figure 4. Oscillogram showing closing-opening operation of the 138-kv vertical-flow breaker on a 220-kv system tested line to ground

Current interrupted 5,900 amperes; three-phase power equivalent to 2,200,000 kva

Reclosing service in this class is not generally used. The main problem is to withstand the heavy short-circuit stresses and rupture the short circuit satisfactorily a minimum number of times. Obviously frequent fault operation on heavy station switches would not represent a practical continuity of service and would need to be corrected without delay. However, it has been found that a switch which will adequately handle a short circuit once will continue to do so any reasonable number of times. This is true, because the contact and oil depreciation with modern interrupters is reduced to negligible amounts and the serviceability of the switch thereby increased.

European Practice. About 12 years ago the European countries faced a problem similar to ours—of greatly increasing the ability of their switchgear along existing lines or departing radically from the older forms. England has kept the dead-tank form, because it readily adapted itself to metal-clad gear. On the other hand, Germany and France, because of shortage of oil and other materials, and because their development of standard breakers had lagged somewhat, decided on new forms. One of these is the expansion breaker sold for 800,000 kva and below at generator voltages with water as a dielectric. Another form is the compressed air breaker, sold for 800,000 kva at generator voltages. Both these forms were developed originally in Germany and have spread to a considerable extent to other continental countries. Each of them, however, has reached approximately the same limitation on kilovolt-amperes as the "De-ion" breaker has in this country for generator service, about 800,000 kva, and this has prevented serious consideration of the European types with us until recently.

Recent American Development. In the past two or three years there has been added pressure of American manufacturers to provide oilless breakers for station service. American manufacturers are prepared to furnish to a greater or lesser degree compressed-air breakers for this service. These have actually been contracted for in sizes 250,000 kva up to 2,500,000 kva for 15 kv and below. This is a new type, and its general construction may be of interest:

1. The grounded metal parts including air tanks, control valves, operating piston, and auxiliaries are all mounted in a subchamber. Inspection and maintenance can be taken care of through the lower doors without entering the high-voltage chamber.
2. Air is taken from the tank in a straight line through the connecting pipe to the arcing chamber and thence through the mufflers to the exhaust spaces in the top of the cubicle. There are no changes of direction in the air, and the loss of air efficiency is eliminated.
3. The contact structure and operating piston are permanently tied together at all times, both closing and opening being secured by reversal of the main piston, but without the necessity of latches and accelerating springs.
4. The full automatic trip-free function is obtained by quick reversal of the air in the operating cylinder and by immediate opening of the dump valve on the closing side. In this way high-speed opening and closing are both obtained with a minimum number of parts, and the mechanism is always in a position to respond to air pressure.
5. Liberal muffler structures are provided on top of the arc chute to eliminate flame and reduce noise.
6. Since the blast valve is mechanically operated from the mechanism, it need not be opened when the breaker is closing, and there is thus a saving of compressed air. Also, the mechanical operation of these valves insures both positive action and that the opening is of a minimum duration. Experience with magnetically operated valves for this purpose shows that they are uncertain in timing and use unnecessary air.

The compressed-air system for use with such breakers is reasonably simple, consisting of a two-stage slow-speed compressor unit mounted on a small tank with a larger storage tank alongside. Cooling coils prevent moisture getting into the ultimate breaker supply, shutoff valves prevent loss of air in the individual breaker tanks in case of supply failure. In important stations several of these compressed-air units will be used, each unit supplying a group of breakers, but the several air systems tied together to afford reserves on each other. At the present time it looks as though water breakers, boric-acid breakers, vacuum breakers, and other experimental substitutes for oil breakers would not be commercialized on a large scale in America. The compressed-air form seems a more logical companion to the small dry breakers previously discussed.

OUTDOOR HIGH-VOLTAGE EQUIPMENTS

Present American Practice. Actual operating voltages in America outdoors cover the range 2,300 up to 287,000 volts, and the old rule of 1,000 volts per mile for transmission length still represents good economy. The outdoor-switchgear field is a very large and important one. It is here that the volumes of oil are greatest because of the insulation spacings required in the apparatus, and therefore one looks naturally for attempts to eliminate the oil circuit breaker. It is a fact, however, that the standard dead-tank oil circuit breaker is well thought of in this country and has been highly developed through co-operative field tests and factory development tests, so that it is a highly reliable unit capable of heavy interruptions, high-speed operation, reclosing service, and freedom from trouble during all weather conditions without the necessity of auxiliary devices to disconnect it from the circuit after interruption. Many of the devices suggested for substitution do not possess all the operating advantages of the standard oil breaker, and this country is loathe to adopt schemes which sacrifice system operation even to a slight degree.

However, the amount of business in the outdoor high-voltage field is so large that manufacturers naturally have investigated any probable substitutes for their present equipment.

An interesting auxiliary development in connection with large outdoor breakers is the use of compressed air for operation. It has been found practical with such a unit to secure faster synchronizing and reclosing time than with magnets or motors on very large sizes. The individual compressor and storage tank at each breaker does away with piping problems from central storage systems.

European Practice. Again because of scarcity of oil for electrical purposes, government regulations on its use, and the cost of metals as compared to porcelains, continental Europe has taken the lead in oil-poor and oilless devices for high-voltage service. England again has retained the practice of dead-tank circuit breakers generally. Automatic reclosing is not generally necessary on the continent because of the use of ungrounded systems with Petersen coils. The principal requirement of an expansion-type oil-poor circuit breaker is ability to interrupt line-to-line short circuits infrequently. Line-to-ground short circuits do not generally call for switch operation.

Another form generally used in Europe is the compressed-air breaker. It is a form arrived at after several years experimentation to eliminate moisture and creepage troubles in the outdoor application of compressed air. It has been in operation as a type from four to five years, but it can hardly be said that there is sufficient background of operation experience to state that it is trouble-free.

Recent American Development. At the present time there is an honest difference of opinion among engineers as to the ultimate development for high-voltage outdoor equipment, assuming that a change from the present dead-tank breaker takes place. Two types are favored, the oil-poor and the compressed-air.

An appreciation of the value of oil as an insulating medium has led some American manufacturers to favor an oil-poor device.

Another form is the Westinghouse vertical flow which, while not in commercial use in America, has been largely produced in Italy and to a less extent in other European countries. It was developed here and was tested on a 220-kv system in the West up to 2,250,000 kva. It has decided advantage for American use, including high-speed reclosure, no external disconnecting devices, high-speed circuit rupture, minimum floor space and oil content. The general satisfaction with standard dead-tank oil breakers has resulted in withholding of manufacture of this type with us.

Compressed air for high-voltage outdoor use is engaging the attention of several manufacturers. A few installations are being made. Compressed air has certain natural advantages for this use, including high-speed interruption, high-speed reclosing and the prompt opening of charging currents and magnetizing currents. Its problems are those of insulation and isolation.

One type of compressed-air outdoor breaker is being produced by the Westinghouse company for field trial installations. With this type it is possible to handle 138 kv in a single interrupter which has been tested up to 2,500,000 kva on a three-pole basis. This interrupter is a composite design using certain features of the longitudinal blast and transverse blast which we feel accounts for its great interrupting capacity. An interesting part of this development is associated with a cold room in which temperatures down to minus 20 degrees Fahrenheit can be secured for proving the operating mechanism and disconnect features under winter conditions. Arcing times are uniformly within one cycle, and arc energy is remarkably low.

Trial installations of 230 kv in this type of compressed air are under consideration. For interrupters it uses two in series, each essentially the same as the 138-kv interrupter referred to above. While five cycles will probably be the industry standard operating time, there will be a demand for three cycles, and compressed air seems to provide for this demand with a minimum of difficulty.

HIGH-POWER LABORATORY

Mention has been made several times of very high values of power for test purposes. These tests have been made possible through the increased laboratory at East Pittsburgh wherein are arranged two machines for parallel operation, each giving approximately 1,000,000-kva symmetrical first-cycle three-phase short circuit. The output then at bus voltage of this laboratory is 2,000,000 kva symmetrical with correspondingly greater amounts of asymmetrical currents. Transformers are provided up to 345,000 volts three-phase and 390,000 volts single-phase. The whole equipment is described in an AIEE paper and provides the largest concentration of laboratory test power available. The use of this equipment is of the greatest value in determining design features and demonstrating complete apparatus. Over 100,000 tests have been made since the original machines were first installed.

REFERENCE

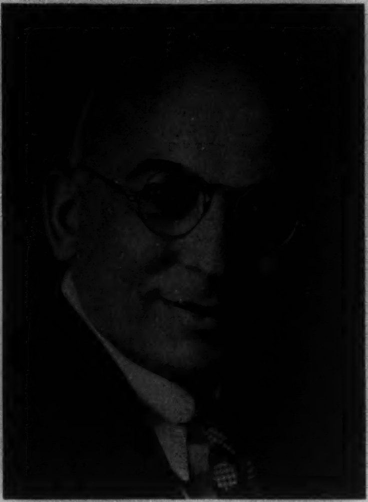
1. **High-Capacity Circuit-Breaker Testing Station, J. B. MacNeill, W. B. Batten.** AIEE *Transactions*, volume 61, 1942, February section, pages 49–53.

Induction Heating Saves Tin

The present tin shortage has accelerated the development by the Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., of a processing line for the electrolytic deposition of tin on steel; high-frequency induction heating speeds the output of tinplate and produces a smooth, shiny, corrosion-resisting finish by causing the coating of tin to reflow. One of the chief advantages claimed for the process is that it uses only one-third as much tin as the method of dipping steel sheets into tanks of molten tin.

After passing through the plating tanks, the tinned steel sheet goes into the inductor heater coil where it is heated to the fusion point of the tin. As the sheet moves through the coil, the minute peaks of tin are melted into an even finish. The melting action stops when the strip moves out of the coil and water cools the metal. High-power vacuum tubes supply high-frequency (200,000 cycles) energy to the inductor.

Several advantages are claimed for the induction heating method of finishing tinplate over the gas-furnace method. First, it avoids cumbersone furnaces, which must be considerably longer than the induction heating coil, and in addition it necessitates no supporting rolls and thus eliminates the possibility of marring the surface. Further, when the end of the coil of the steel strip approaches the inductor heating coil the operation must be slowed to weld on another strip. The heat generated can be adjusted to compensate for the loss of speed, while in a furnace this cannot be done. An additional advantage of the induction heating method is that its high-frequency unit can be put directly into the electrolytic tinning line and can operate as fast as the strip can be plated.



I Am an American

ARAM BOYAJIAN
FELLOW AIEE

I LOOK back over half a century to refresh my memory of what being an American has meant to me progressively through all these years. Born in Armenia, I derived my earliest conception of America from story tellers, and it was about as fantastic as Alice's Wonderland. At the age of 21, while teaching arithmetic in a school, the thought occurred to me one winter morning that I should go to this romantic country. Within a few hours I was on the way. What? Such haste? Doesn't one wait to the end of the school year? Of course not; that is, not if one has indulged in a little free speech the night before in a public lecture at which unexpectedly some Turks were present, and this morning one has been notified to go to the government palace and hand the manuscript of his talk to the pasha. To the Armenians, a phantom inscription on the palace door said: "Enter. No exit." In the Turkish Empire, only the Turks took their time; others hustled.

While the issue of freedom of speech—one of the four freedoms of the Atlantic Charter—was the immediate occasion for my taking the first formal step toward becoming an American, the other three freedoms also were inseparably involved in it. Freedom of worship, freedom from periodic wholesale looting, and freedom from periodic massacres. I need not repeat here once more the story of the 600 years of oppression of the Armenians in Turkey, a history which has ended largely because of the extinction of the Armenians as a community in the Turkish Empire.

As if to impress on me with the greatest possible contrast, the differences between life in the Turkish Empire and life in America, destiny guided my steps to Swarthmore, a small peaceful Quaker college town in the suburbs of Philadelphia, Pa. There I worked at tailoring and studied, studied human nature along with electrical engineering. What a contrast there was between the Turkish conception of life and the Quaker! As I was learning what is approved engineering practice, I was learning also what is approved American practice. Every year, even after I was naturalized, I was learning still more reasons, besides the four freedoms, why I would want to be nothing but an American.

During my first week in Philadelphia, the public parks and parkways, museums, various other city improvements, and public services astounded me. The mayor's slogan was "The City Beautiful." I asked an old timer how heavy were the taxes. "No personal tax," he said, "only real estate is taxed; so you pay tax indirectly in your rent." And rents were really reasonable. The Federal Government did not even tax real estate; it paid its expenses mostly from duty on imports. Honest government explained it all. Of course, I heard about a "corrupt" Vare machine in Philadelphia, but when my class visited a pumping station built by him, we found it a fine job, and Vare was proud enough of it to engrave his name prominently over the entrance. Evidently, contract corruption in the United States did not mean what it would have meant in the old country.

At a men's class, my first or second Sunday in America, a former secretary of the Commonwealth of Pennsylvania spoke eloquently on how Americans are being robbed by intrenched privilege. "Every time you buy a pound of sugar," he said, "the hand of privilege slips into your pocket and robs you." He repeated this form many times, changing each time the name of the article. I asked the man next to me how much a pound of sugar costs. "Five cents," he said. I lost all interest in the talk; the speaker was painting America black evidently as propaganda for lower tariff.

I was surprised at the appreciativeness of the average American. If you did a good job for him, he admired it loudly. Strange! In the old country, no matter how fine a job it was, we said to the cobbler, "Do you think those heels will stay on? The soles look like cardboard to me." The theory was that if you admitted it to be a fine job, he would charge more, as there was no standard price on anything. This situation also led to every business transaction's becoming a haggling contest. People forgot that its original purpose was to save money and indulged in it to prove their superior wits.

This article is the text of an address delivered before the Daughters of the American Revolution of Pittsfield, Mass., and broadcasted through radio station WBRK on an "I Am an American" program on May 17, 1942.

Aram Boyajian is an electrical engineer, General Electric Company, Pittsfield, Mass.

This may have sharpened their wits somewhat, but it also developed argumentativeness and faultfinding in everybody, and made human relations more difficult. In America people had discovered a better way of spending their time than haggling or arguing or faultfinding. I saw in the American experience that the art of appreciation, properly practiced, made not only for better relations all around, but also did actually save money. The workman, elated, was actually more amenable to a lower level of compensation. In politics there was of course faultfinding of a kind people called mud-slinging; but while here the worst thing that a respectable politician would call his opponent would be "an agent of big business," over there he might call him a robber of church silver.

I discovered that generosity was another characteristic of standard American practice. The grocer made sure that the scales tipped clearly in your favor; the satisfied customer would tell him sometimes not to bother about the change; the tailor did more repairs on your clothes than was included in the agreement; employers gave raises to their men without any demand from them; the department store took back the fur coat without asking if it had been worn to a wedding; and all drives for charitable purposes went over the top. It was a fantastic generous world and it ran so smoothly.

I discovered that good sportsmanship was another characteristic of good American practice. No particular word for it existed in the Near-East languages.

That the American is law-abiding was another revelation to me. While in the old country the law was made by an overlord for the purpose of hamstringing you, and you had no liking for it; here it was made by your own representatives to help make your pursuit of happiness run more smoothly, and you naturally conformed to it.

The co-operativeness of the American was a particular surprise to me. Competitors in America co-operated with each other better than partners did in the old country; and I was amazed that laws were passed here against too much co-operation. I believe America is the only country where businessmen are prosecuted for too friendly competition. Recent un-American labor troubles were of European origin and conception. I count on America's genius at co-operation to work out complete labor-capital co-operation in a few years.

Finally, I learned more about the missionary spirit in the American make-up and destiny. All of the Americans I had seen in the old country were missionaries. I had studied in their schools. So my earliest notion of an American was that of a missionary. Years have confirmed and put deeper meaning into this notion.

When I was naturalized a quarter of a century ago, I wrote home with youthful enthusiasm: "Now, Mother, I am an American, full-fledged and complete—as good as the missionaries, even as Doctor Dodd."

The answer came back in due time, "My son, your father and I are very happy that you are no more a Turkish subject, but a free American citizen. But to be a complete American must take time. You can't be as complete an American as the missionaries, as Doctor Dodd, until you are able to render to the world the kind of service that they are rendering. Don't worry about taking us there; you put your effort toward learning a profession and take your place among other Americans, and we will be happy no matter where we are."

You see, the only Americans she ever knew were missionaries; and to her, as to everybody else in her section of the world, Doctor Dodd was the angel of the Lord dispensing far and wide the blessings of American medicine and surgery. She had the notion that all Americans are missionaries, in one sense or another, sent by God to this unfortunate world.

That was the last I heard from her, and the time was the first World War. With my father and millions of other Armenians she was deported into the desert of Der El Zor as a menace to the safety of the Ottoman State; and her group, I learned later on, was crowded into a large enclosure, sprinkled with oil, and set on fire.

Well, she had gone and left me with a big order—to try to become an American like her model, Doctor Dodd of blessed memory. I spent many uneventful years after that, doing the unromantic daily tasks of my profession with the General Electric Company, until a few years ago a little incident happened that eased my mental burden a bit. My wife and I were looking for the cottage of a friend at Onota Lake, Mass., and seeing a lady, we approached her for information. She was elderly, with all-white hair, gracious in manner, and distinguished in appearance.

She gave us the information, and then she said, "You are Mr. Boyajian, aren't you?" I said, "Yes," a bit surprised. "I know you," she said, "from the first reception the Daughters of the American Revolution gave to new citizens. My name is Mrs. Irving Gamwell, regent of the DAR. I have watched you ever since, and you have been a good American."

A second World War has broken upon us now, and the meaning of being an American is being discussed widely and clarified more and more. It is penetrating now even into the heads of some of the isolationists that what the Bible says about individuals is true also about nations. A nation cannot live for itself alone or die for itself alone; the nation which tries to save itself alone is likely to perish, but one that is willing to risk itself for the larger good will survive. America is realizing its mission as a participant in the present struggle.

Were General MacArthur's Biblical allusions to our soldiers at Bataan overgenerous? Ask the Australians, the Dutch, the Filipinos, the Chinese. They'll say, "A thousand times, no." To those people, as to all other oppressed or threatened peoples of the world, an American now means a fighting missionary for the four freedoms of the Atlantic Charter.

INSTITUTE ACTIVITIES

Aid to War Effort to Be Theme of National Technical Meeting

Maximum aid to the war effort will be the principal theme for most sessions and conferences to be held during the AIEE national technical meeting in New York, N. Y., January 25–29, 1943, with headquarters in the Engineering Societies' Building. The Institute of Radio Engineers has been invited to hold their meeting concurrently. Thursday, January 28, will be devoted primarily to IRE sessions, which will be open to AIEE members, in both morning and afternoon. IRE members also are cordially invited to attend any of the AIEE sessions. The day's activities will culminate in a joint AIEE–IRE evening meeting.

Arrangements are being made for a general-session program which will present the important subject of technical manpower requirements as an aid to the war effort. Prominent speakers will present both the military and industrial phases of this subject.

MEDAL PRESENTATIONS

It is expected that the Edison Medal will be awarded in December and it will be presented Wednesday evening, January 27, 1943, together with the John Fritz and Hoover Medals in the engineering auditorium.

The John Fritz Medal was awarded to Doctor Willis R. Whitney (A '01) (*EE, November '42, p. 574, 582*) "for distinguished research, both as an individual investigator and as an outstanding and inspiring administrator of pioneering enterprise, co-ordinating pure science with the service of society through industry."

The Hoover Medal was awarded to Gerard Swope (F '22) (*EE, November '42, p. 874–5, 582*) with the following citation: "engineer and distinguished leader of industry, ever deeply interested in the welfare of his fellowmen, whose constructive public service in the field of social, civic, and humanitarian effort has earned for him the Hoover Medal for 1942."

TECHNICAL SESSIONS AND CONFERENCES

Most of the sessions and conferences of aid to the war effort will deal with getting the most out of existing equipment in order to conserve copper and other critical materials. Thus the session on capacitors will point out the savings in generator capacity and critical materials that may be effected by a more intelligent use and application of capacitors on power-transmission systems. Another session on transformer overloading will deal with the thermal protection, cooling by forced circulation, the effect of ambient temperatures, and transformer overloads for cases not covered by general rules, all of which tend toward getting the most out of existing transformers without destruction of equipment.

Among the conferences which will aid the war effort are the following: cables and overload capacities to conserve copper, changes in the design and operation of substations, power-generating equipment, up-to-the-minute discussions of defense lighting activities and developments, and guides for the selection of general-purpose motors. Since the amount of copper used to serve motors, in switches, control equipment, and distribution systems is proportional to the nameplate ratings and is from two and one-half to seven times the weight of copper in the motors a very large saving in critical materials may be made by a careful selection of motors of proper size.

As already announced (*EE, October '42, p. 527*), other "guides" or "general principles" for the selection and operation of electric equipment are in preparation in co-operation with the AIEE standards committee. Some of these guides will serve as a basis for discussions in the sessions and conferences.

The personnel of the committee making arrangements for the meeting is as follows:

Chairman, C. R. Jones (M '30); F. A. Cowan (M '29); W. S. Hill (M '30); M. D. Hooven (M '30); A. E. Knowlton (F '30); C. S. Purnell (M '35); R. L. Webb (M '35); C. C. Whipple (M '26).

Future AIEE Meetings

National Technical Meeting
New York, N. Y., January 25–29, 1943

District Technical Meeting
Pittsfield, Mass., April 8–9, 1943

District Technical Meeting
Kansas City, Mo., April 28–30, 1943

National Technical Meeting
Cleveland, Ohio, June 21–25, 1943

Board of Directors Meets

The regular meeting of the board of directors of the American Institute of Electrical Engineers was held at Institute headquarters, New York, N. Y., October 23, 1942.

The election by the executive committee, under authority of the board of directors, of C. A. Powel (F '41) as vice-president of the Institute, representing the Middle Eastern District, for the unexpired term, ending July 31, 1943, of N. S. Hibshman (F '41), who resigned because of his removal from the District, was reported. The board confirmed this action (*EE, October '42, p. 532; November '42, p. 574*).

Upon recommendation of the Providence Section and the executive committee of the North Eastern District, the board authorized changes in location and dates of the 1943 technical meeting of that District to Pittsfield, Mass., April 8 and 9. It was originally scheduled to be held in Providence, April 7–9.

Upon request of the technical program committee, the chairman of the standards committee was made an ex-officio member of the technical program committee.

Upon recommendation of the technical program committee and the technical committees concerned, the board authorized the change in status of the joint subcommittee on electronics to that of an AIEE technical committee on electronics, with the following definition of scope:

> "Treatment of all matters in which the dominant factor is electronics, particularly those dealing with the design, characteristics, or behavior of devices of an electronic nature, or of the circuits associated with them. The treatment of complete equipments or systems may be considered within the scope of this committee if they depend predominantly on electronic devices for their operation. If the field of application of such equipments or systems falls also within the scope of other technical committees, action with those committees may be joint."

Authorization was given to the technical program committee to arrange with the Institute of Radio Engineers for joint sessions at the national technical meeting of the AIEE to be held in New York, N. Y., January 25–29, 1943.

Upon recommendation of the standards committee, the board approved, for submission to the American Standards Association a revision of the American Standard on "Lightning Arresters for A-C Power Circuits" (AIEE, 28; ASA, *C*62), developed by a subcommittee of the AIEE committee on protective devices, and approved the appointment of AIEE representatives on other standardizing bodies, as follows:

Walter Mikelson (A '40) to the Sectional Committee on Classification and Designation of Surface Qualities, *B*46.

H. W. Bousman (M '36) to Sectional Committee on Specifications and Methods of Test for Electrical Insulation Materials, *C*59 (to replace J. A. Scott (M '34) who has entered the United States Army.)

C. A. Powel (F '41) to replace D. F. Miner (F '40) (who has entered the United States Army) on Sectional Committee *C*67, "Voltages Below 100."

Edward Bennett (F '18) as a representative of the Institute on Sectional Committee *Z*10, "Letter Symbols and Abbreviations for Science and Engineering" (transferring him from the status of Member-at-Large).

J. T. Lusignan, Jr. (M '34) as chairman of Sectional Committee *C*68, "Sphere Gap Standardization," to succeed E. J. Rutan (M '29), resigned.

C. H. Willis (F '42) as chairman of the Sectional Committee on Mercury-Arc Rectifiers, C34, replacing E. L. Moreland (F '21), resigned.

The Institute bylaws were amended as follows, to conform with the action of the board of directors on August 7, 1942, abolishing the Institute policy committee:

Section 65: The name of the Institute policy committee deleted from the listing under the heading "General Committees."

Section 73: The entire section deleted.

In accordance with section 22 of the bylaws, five members of the board of directors were selected to serve on the national nominating committee, namely, M. S. Coover (F '42), M. Eldredge (F '33), C. R. Jones (M '30), T. G. LeClair (F '40), E. T. Mahood (F '36).

Representatives of the Institute were appointed as follows:

Alfred Noble prize committee—Robin Beach (F '35), for the five-year term beginning January 1, 1943.

Standards Council, American Standards Association—J. R. North (F '41), for the three-year term beginning January 1, 1943; H. E. Farrer (A '21), H. L. Huber (M '23), and E. B. Paxton (M '25) as alternates for the calendar year 1943.

The personnel of the committee on award of Institute prizes, which awards the national prizes, was increased by the addition of the chairmen of the committees on communication, electrical machinery, power transmission and distribution, and protective devices.

Other actions taken by the board were as follows:

Minutes of the meeting of the board of directors held August 7, 1942, were approved.

The following actions of the executive committee on applications for election, transfer, and Student enrollment, as of September 29, 1942, were reported and confirmed: 7 applicants transferred to the grade of Fellow; 25 applicants transferred to the grade of Member; 21 applicants elected to the grade of Member; 130 applicants elected to the grade of Associate; 229 Students enrolled.

Recommendations adopted by the board of examiners at meetings held September 24 and October 15, 1942, were presented and approved. Upon recommendation of the board of examiners, the following actions were taken: 10 applicants were elected to the grade of Member; 68 were elected to the grade of Associate, and one Associate was reinstated; 311 Students were enrolled.

Monthly disbursements, reported by the finance committee, were approved as follows: August, $43,673.67; September, $19,285.97; October, $35,553.27.

A budget for the appropriation year beginning October 1, 1942, was adopted as recommended by the finance committee.

Those present at the meeting were:

President—H. S. Osborne, New York, N. Y.

Past president—R. W. Sorensen, Pasadena, Calif.

Vice-presidents—A. G. Dewars, Minneapolis, Minn.; J. Elmer Housley, Alcoa, Tenn.; C. R. Jones, New York, N. Y.; E. T. Mahood, Kansas City, Mo.; K. B. McEachron, Pittsfield, Mass.; C. A. Powel, East Pittsburgh, Pa.; E. W. Schilling, Bozeman, Mont.; Walter C. Smith, San Francisco, Calif.

Directors—M. S. Coover, Ames, Iowa; M. Eldredge, Washington, D. C.; C. M. Laffoon, East Pittsburgh, Pa.; T. G. LeClair, Chicago, Ill.; W. R. Smith, Newark, N. J.; R. G. Warner, New Haven, Conn.

National treasurer—W. I. Slichter, Schenectady, N. Y.

National secretary—H. H. Henline, New York, N. Y.

By invitation, Chairman John C. Parker of the committee on co-operation with war agencies was present during a part of the meeting.

The 1942–43 Institute Budget

In the accompanying tabulation will be found a report of the income and expenditures of the Institute for the appropriation year ending September 30, 1942, and the budget as recommended by the finance committee for the ensuing year. Approval of this budget by the board of directors, at its meeting held on October 23, 1942, was given with the understanding that practically all activities would now conform to the definition of Institute wartime policies (*EE, September '42, p. 477*). For example,

Institute Income and Expenses for the Year Ending September 30, 1942, and Budget for Year Ending September 30, 1943

	Actual Income and Expenses, Year Ending 9-30-42	Budget for Year Ending 9-30-43
Income		
Dues	$228,038.16	$210,000.00
Students' fees	14,239.25	13,000.00
Entrance and transfer fees	8,830.97	8,000.00
Advertising	71,000.83	70,000.00
Elec. Engg. — non-mem. subscriptions	9,716.67	9,000.00
Trans. subscriptions	6,119.10	6,000.00
Miscellaneous sales	34,714.80	30,000.00
Interest on securities	7,696.09	6,000.00
Total	$380,355.87	$352,000.00
Expenses		
Publications:		
Text matter (Elec. Engg. & Trans.)	80,946.80*	71,065.00†
Trans. Supplement	1,525.31	750.00
Preprints	7,357.48	8,300.00
Advertising section—Elec. Engg.	27,616.91	28,200.00
Year Book	7,367.29	7,300.00
Miscellaneous expense	1,301.05	1,500.00
Institute meetings	14,111.74	14,800.00
Institute Sections:		
Appropriations	26,831.06	28,000.00
Other expenses	6,009.21	6,450.00
Institute Branches:		
Meetings expenses	922.84	1,100.00
Other expenses	2,521.65	2,950.00
Committees:		
Code of prin. prof. conduct		50.00
Edison Medal	188.10	200.00
Finance	771.23	750.00
Headquarters	27.75	100.00
Lamme Medal	20.00	275.00
Membership	9,068.32	9,200.00
Pension Fund	250.00	500.00
Research		100.00
Standards	8,517.97	11,050.00
Standards — Elec. Definitions	18,691.16	1,000.00
Technical committees	455.10	500.00
Co-operation with war agencies		500.00
Traveling Expenses:		
Geo. Dist. exec. committees	3,609.23	4,250.00
Section delegates to summer conv.	4,665.41	5,800.00
Counselor delegates to summer conv.	510.96	800.00
Dist. secys. to summer conv.	83.98	525.00
District Student conferences	6,313.80	7,950.00
President's appropriation	1,576.76	1,500.00
Vice-presidents	787.95	1,060.00
Board of directors	6,653.80	7,400.00
National nominating committee	1,121.49	1,200.00
Past presidents and other AIEE representatives		515.00
Administration:		
Headquarters' salaries	37,466.00	39,950.00
Postage	4,384.36	4,500.00
Stationery & printing	4,069.96	4,250.00
Office equipment	513.18	500.00
Trav. expense, bank charges, insurance, misc. supplies and services	4,398.07	4,750.00
Paper prizes	764.00	1,200.00
Joint activities:		
Amer. Co-ord. Com. Corrosion	25.00	25.00
American Standards Association	1,500.00	1,500.00
ECPD	1,700.00	1,700.00
Engineers Defense Board	276.67	
Engg. Foundation research projects:		
Impregnated paper insulation	250.00	
Insulating oils and cable saturants	250.00	250.00
Welding	250.00	250.00
Engg. Soc. Personnel Service, Inc.	1,152.00	1,338.00
Engg. Soc. Library	10,261.44	10,500.00
Hoover Medal		175.00
John Fritz Medal	50.00	50.00
N.F.P.A. Dues	60.00	60.00
Production clinics	130.00	
Radio broadcasts	350.00	
United Engg. Trustees building assessment	10,984.80	10,985.00
U. S. Natl. Com. I.C.I.	100.00	100.00
Miscellaneous printing, etc.		8,000.00
Authors' reprints	4,199.38	
Reprints of standards	1,949.00	
Miscellaneous	1,655.97	
Other expenses:		
Membership badges	2,325.17	2,500.00
Legal services	250.00	250.00
Pension Fund Reserve	8,000.00	10,000.00
Prepaid Elec. Engg. expenses (text paper and env. in storage)	8,997.75	9,000.00
Depreciation Reserve Fund	109.55	
Unappropriated:		
Reserved for contingencies and additional items		14,527.00
Total	$346,246.65	$352,000.00

* Actual cost $85,637.00 including expenses paid preceding year.

† Actual budget of $80,495.00 including prepaid expenses.

it is expected that Institute publications will contain, so far as possible, material which is closely related to the war effort, and that a similar policy will be followed in the selection of technical papers for presentation at national and District technical meetings, the arrangements for which will omit the usual social activities.

It will be noted that the appropriations granted for the principal activities—publications, meetings, Sections, Branches, committees, and agencies supported by the Institute jointly with other engineering societies—closely correspond with the expenditures for last year; some items are increased slightly to cover advances in prices effected since October 1941. Increases in salaries paid to headquarters' staff, affecting fifteen persons, account for approximately $3,000 of the new budget; $1,500 additional is provided in the appropriation of the administrative department for one addition to the staff.

Other budget items deserving comment are: an increase in the rate of traveling expenses for delegates to Institute meetings and to the District conferences for which such allowances are granted (from eight and one half to nine cents per mile, one way); the restoration of all national and District paper prizes; and increase of $2,000 in the annual payment to the pension fund reserve established two years ago. The budget includes $14,500 as unappropriated, for contingencies and additional expenses which may develop during the year.

Income for the coming year has been estimated below that realized last year, to allow principally for losses in dues revenue from members in military service and from overseas members. On the whole, the budget reflects the desire of the board of directors to place proper relative emphasis on the different phases of Institute activities, as modified by present-day requirements, and to limit annual expenditures to the amount of anticipated income. For the preceding year the board voted to transfer $20,000 of excess receipts over expenditures from the general funds to the reserve capital fund of the Institute.

Members wishing further particulars regarding the details underlying the various activities provided for in the budget are referred to the annual report of the board of directors for the fiscal year which ended April 30, 1942 (*EE, July '42, pp. 368-81*).

All AIEE National and District Cash Prizes Restored

At its meeting on October 23, 1942 (*this issue, p. 616*) the AIEE board of directors made provisions for the restoration of all National and District cash prizes to be awarded for the current year. Cash prizes previously were restored for all national prizes and two of the four District prizes.

All technical papers presented before the Institute are eligible under the AIEE paper prize regulations for competitive consideration for one or more of the established prizes, regardless of whether presented before a Branch meeting, a Section meeting, a District meeting, or a national convention, the several classes of prizes providing for equitable competition.

National prizes awarded are:

1. Prize for best paper in each of the following classes ($100 and certificate):
    Engineering practice
    Theory and research
    Public relations and education
2. Prize for initial paper ($100 and certificate)
3. Prize for Branch paper ($100 and certificate)

District prizes that may be awarded annually in each of the geographical Districts of the Institute are:

1. Prize for best paper ($25 and certificate)
2. Prize for initial paper ($25 and certificate)
3. Prize for Branch paper ($25 and certificate)
4. Prize for graduate student paper ($25 and certificate)

PERIODS OF COMPETITIONS

The national best paper prizes and the national initial paper prize are awarded for papers presented during the calendar year, except best paper prize in the class of public relations and education. In this class the award is for a paper presented subsequent to those considered at the time of the last previous award in this field and prior to the end of the calendar year. For the national prize for Branch paper only papers presented during the preceding academic (college) year, July 1 to June 30, inclusive, will be considered.

The District prize for best paper and the District prize for initial paper are awarded only for papers presented subsequent to the period covered by the last previous award in the class and prior to the end of the last calendar year. The District prize for Branch paper and the District prize for graduate student paper are awarded only for papers presented subsequent to the period covered by the last previous award in the class and prior to the end of the last academic (college) year, July 1 to June 30, inclusive.

TIME OF AWARDS

All of the national prizes and the District prize for best paper as well as the District prize for initial paper shall be awarded prior to May 1 of the succeeding year. The District prize for Branch paper and the District prize for graduate student paper will be awarded as determined by the District executive committee and announced to all Branches in the District.

HOW TO SUBMIT PAPERS

For the national prizes all papers approved by the technical program committee which were presented at the national conventions or District meetings will be considered for the best paper prizes and the initial paper prize without being formally offered for competition. All other papers which were presented at Section or Branch meetings must be submitted in triplicate with written communications to the national secretary on or before February 15 of the following year, stating when and where the papers were presented.

For the District prize for best paper and the District prize for initial paper papers shall be submitted at least in duplicate by authors or by officers of the Section, Branch, or District concerned to the District secretary on or before February 1. The District secretary will refer the papers either to the District executive committee or a committee appointed by the District executive committee authorized to make the awards. For the District prize for Branch paper and prize for graduate student paper papers shall be submitted at least in duplicate by authors or by officers of the Section, Branch, or District concerned to the District secretary in accordance with dates of closure and of award, as fixed by the District executive committee and announced to all Branches in the respective Districts.

The basis of evaluating all student papers has been changed to bring it more in conformity with the objectives of such papers. Those wishing further information may obtain a booklet entitled "National and District Prizes" from AIEE headquarters, 33 West 39th Street, New York, N. Y.

Bibliography to Be Published on Automatic Stations

The fourth bibliography of technical literature entitled "Bibliography on Automatic Stations, 1930–1941" is soon to be issued by the Institute. This publication sponsored by the AIEE committee on automatic stations supplements earlier bibliographies on the subject published previously in AIEE *Transactions*.

The entries in this bibliography are numbered consecutively by sections and listed alphabetically by years. The material is divided into the following sections:

General; supervisory and remote control; telemeter and telemetry; automatic and remote-controlled switches and switchgear; automatic features of generating stations using fuels; automatic boiler and combustion control; automatic hydroelectric plants; automatic substations.

The "Bibliography on Automatic Stations, 1930–1941" will be obtainable from AIEE headquarters, 33 West 39th Street, New York, N. Y., at 25 cents per copy for AIEE members (50 cents to nonmembers) with a discount of 20 per cent for quantities of 10 or more mailed at one time to one address. Remittances, payable in New York exchange, should accompany orders.

DISTRICT • • • • •

District 2 Branch Counselors, Chairmen, and Officers Meet

The Student Branch meeting of the AIEE Middle Eastern District (2) held on October 16, 1942, at the University of Pittsburgh (Pa.) was attended by 73 students and 14 professors.

At the conference of counselors held during the meeting the following were appointed to serve on the district nominating committee:

L. R. Culver, University of Cincinnati (Ohio); L. J. Hodgins, University of Maryland, College Park; F. W. Smith, Lafayette College, Easton, Pa.

It was decided that the next meeting would be held at Pennsylvania State College. The exact date of the conference is to be announced.

A discussion of the effects of the accelerated wartime programs on Branch activities revealed that all schools are not following the same plan. It was urged that a greater effort be made to have juniors and sophomores take a more active part in Branch activities.

At the meeting of District 2 student Branch officers held on October 16, 1942, student chairmen of the various AIEE Branches in the District reported on the Branch activities of the past year. It was decided at this meeting that the "Shunt," official student publication of District 2 be published in six issues during the coming year. Branches were requested to support the publication editorially and financially.

A joint meeting of Student Branch chairmen and counselors was also held.

SECTION • • • • •

Washington Section Opens Season With Talk on Rubber

The AIEE Washington Section held its opening meeting of the 1942–43 season on October 13, 1942. Following dinner at the Harrington Hotel, R. A. Schatzel (A'28) director of research, General Cable Corporation, Rome, N. Y., and chairman of the technical advisory committee to the rubber and rubber products branch of the War Production Board, spoke on "Some Engineering Aspects of Synthetic Rubber." Discussing the types, properties, and availability of synthetic rubber, its properties as insulation, and its uses from an engineering point of view, he pointed out that synthetic rubbers should be considered as different types of rubber, not as separate materials different from natural rubbers. He stated further that each kind of synthetic rubber has properties that adapt it for particular uses. After the meeting refreshments were served.

ABSTRACTS • • •

Basic Sciences

43-11—Formulas for the Calculation of the Inductance of Linear Conductors of Structural Shape; *Thomas J. Higgins (A'40). 15 cents.* The formula for the geometric mean distance between two unlike rectangles with corresponding sides parallel but otherwise arbitrarily located is derived by evaluating the appropriate quadruple integral. Conjoining this formula (which encompasses, as special cases, all hitherto known formulas for the geometric mean distance between parallel-sided rectangles) with the cardinal theorem of geometric mean distance theory yields very general formulas that afford the inductance of single or of polyphase circuits constructed of linear conductors with right cross sections composed of arbitrary configurations of parallel-sided rectangles. As illustrations, specific formulas are obtained for:

1. The inductance of single-phase circuits comprised of two symmetrically located conductors, these latter being

(*a*). Channels back to back.

(*b*). Split rectangular tubular conductors.

(*c*). Two I-beams.

2. The reactance drop in an individual conductor of a three-phase circuit comprised of three equilaterally spaced, rectangular tubular conductors.

A short but comprehensive résumé of the existent literature on formulas for the calculation of the inductance of linear conductors of solid or tubular rectangular cross section is included in the paper.

Electrical Machinery

43-2—A Useful Equivalent Circuit for a Five-Winding Transformer; *L. C. Aicher, Jr. (A'37). 15 cents.* The possibilities of multiwinding transformers with five windings have been discussed by power-system engineers. Heretofore, serious consideration of such an equipment has been curtailed because of an inadequate five-winding equivalent circuit. This paper presents a practical equivalent circuit together with a development of the necessary formulas. All formulas make use of quantities that can be easily measured at the transformer terminals. Numerous mathematical manipulations in the development have been omitted from the paper in the interest of brevity. A fundamental objective has been to keep the equivalent circuit suitable for the calculating board. Examples show how the various parameters are evaluated. One example demonstrates the factors affecting the voltage regulation of a five-winding transformer when connected to two sources of energy.

43-3—A New Type of Adjustable-Speed Drive for A-C Systems; *A. G. Conrad (M'40), S. T. Smith (Enrolled Student), P. F. Ordung (Enrolled Student). 15 cents.* This paper describes the theory and characteristics of a new type of polyphase adjustable-speed motor and its control circuit. The system provides an a-c drive that has characteristics comparable to those of the Ward Leonard system used on direct currents. The control circuit provides starting as well as speed adjustments from zero to one hundred per cent of rated value. The power factor of the motor is either leading or very close to unity over its operating range, and consequently the efficiency is high. Low costs of this type of motor and its control system, the large speed range, the facility for rapid reversal by regeneration, and its high starting torque make it suitable for some industrial applications.

43-4—Performance Calculations on Tapped-Winding Capacitor Motors; *P. H. Trickey (M'36). 15 cents.* The tapped-winding capacitor motor is a capacitor motor with an extra control winding which is put in series with the motor to obtain a lower operating speed. The high-speed performance may be calculated by one of a number of recognized methods. The low-speed performance may be determined approximately by calculating the performance at a low fictitious main-winding voltage and adding to this voltage the proper impedance drops of the control winding. By trial and error this total voltage is made to equal the line voltage.

43-6—Analysis of Rating Methods for Intermittent Loads; *R. E. Hellmund (F'13) (deceased). 15 cents.* The economies of rating structures for electrical equipment from the viewpoints of manufacturing and application are considered. Methods for dealing with periodic load applications are reviewed. The present methods have certain limitations for some practical applications. An improved method for applying motors to load cycles where the motor stops and ventilation changes in the no-load part of the cycle is developed. This method is based on certain motor-loss ratios, that is, the relation between the losses dissipated with standard temperature-rise running and at standstill and the losses at the nominal or short-time rating. It permits the application of service-factor or short-time ratings to periodic loads with more exactness. Attention is called to the wide variations in permissible rms current on periodic loads for different loss ratios and changes in ventilation on a motor.

Instruments and Measurements

43-1—An Instrument for the Determination of Contact-Making and Breaking Time; *Walther Richter (F'42), W. H. Elliot (A'40). 15 cents.* Hitherto for the measurement of the arcing time of contacts, opening or closing a circuit, oscillograms have had to be taken. This procedure is tedious and time consuming, especially when many observations are required. An electronic device has been developed which indicates the time of arcing on a direct-reading instrument. Four ranges, 2, 5, 10, and 20 milliseconds, for full scale deflection are available. Use is made of the well-known method of charging a condenser with a definite current, the charge accumulated being a measure of the duration of current flow. The novel feature is the method of causing the charging current to flow only during the arcing time of the contacts under investigation. A Wheatstone bridge with a vacuum tube in one arm is used to indicate the condenser charge on the direct-reading instrument.

Power Transmission and Distribution

43-7—Abnormal Overvoltages Caused by Transformer Magnetizing Currents in Long Transmission Lines; *H. A. Peterson (M '41), T. W. Schroeder (A '37). 15 cents.* It has been found by means of the transient analyzer that certain combinations of transmission-line length and system reactance can give rise to overvoltages of considerable magnitude when a lightly loaded transformer is connected to that transmission line. Such a system is most susceptible to overvoltage phenomena when the transformer is connected to the receiving end of that transmission line, although, theoretically, similar phenomena may occur for any transformer location including the transformer at the sending end. These overvoltages are in addition to the normal fundamental-frequency voltage rise and are of a combination subharmonic and harmonic nature. A comprehensive analysis of representative circuits with and without series-capacitor compensation has been made to determine the regions of abnormal behavior and the effectiveness of various practical methods for safely limiting such overvoltages. Such practical means for controlling the overvoltage include proper switching and relaying arrangements, the synchronous condenser, shunt reactors, and resonant shunts. These results will be of considerable importance from the standpoint of designing and operating systems involving the transmission of power over distances in the practical range above 200 miles.

43-9—Equivalent Circuits for Oscillating Systems and the Riemann–Christoffel Curvature Tensor; *Gabriel Kron (A '30). 15 cents.* It is shown that a necessary though not sufficient condition for the existence of a physical model, corresponding to a given set of equations, is that the set should be a tensor equation. This follows from the fact that only quantities that are tensors can be measured by instruments. The conclusion is therefore reached that only equations that are in tensor form can be set up on the a-c calculating board. The principle is illustrated by setting up equivalent circuits with the aid of tensorial hunting equations for the determination of the steady-state, hunting, and self-excitation performance of two interconnected instrument-selsyns, of two salient-pole synchronous machines, and of a capacitor-compensated transmission line connected to a salient-pole synchronous machine. A companion paper, 43-8, contains the results of an extended study made on the a-c network analyzer with the aid of one of the equivalent circuits developed here.

43-8—Self-Excited Oscillations of Capacitor-Compensated Long-Distance Transmission Systems; *R. B. Bodine (A '37), C. Concordia (M '37), G. Kron (A '30). 15 cents.* This paper presents the results of an investigation of regions of possible instability which may be encountered in the transmission of electric power for long distances over series capacitor-compensated lines. The limits of stability, hunting, and self-excitation applicable during normal steady-state operation are determined. The method described can be used to determine the effects of length of line, amount of compensation, machine and line characteristics, and so forth. Results showing the effects of compensation and of machine characteristics and loading are given. The system performance was determined by calculations and measurements on equivalent circuits set up on the a-c network analyzer. The method used is believed to have certain advantages, particularly in the range of system parameters which can conveniently be considered, and in the accuracy of representation of the system, over those used in previously described investigations. It is concluded that the types of instability considered are avoidable in practical applications of long-distance electric-power transmission if attention is paid to proper design. An effective amortisseur is particularly desirable.

43-10—Dielectric Recovery Characteristics of Large Air Gaps; *G. D. McCann (A '38), J. J. Clark (A '40). 15 cents.* The rates at which large air gaps recover dielectric strength after discharging short-duration surge currents comparable to those produced by lightning are of fundamental importance in relation to the mechanism of natural lightning and in determining power-system performance. For the study of this problem a method of synchronizing two surge generators has been developed so that a surge discharge can be produced and controlled surge voltage applied after specified time intervals. By this means a study of the rate of dielectric recovery and the visual mechanism of successive discharges has been made for a wide range of initial discharge currents. The data obtained are compared with the mechanism of multiple strokes and are applied to the problem of determining the probability of transmission-line lightning flashovers producing an outage and to the minimum safe reclosing times after circuit interruption.

Protective Devices

43-5—Correlation of System Overvoltages and System Grounding Impedance; *working group on correlation of system-grounding impedance of the committee on protective devices. 15 cents.* The various causes of system overvoltages other than lightning and the effect of the different classes of system grounding on these overvoltages are examined in this paper. If in a given system or portion thereof, $X_0/X_1<3$ and $R_0/X_1<1$ for all fault locations and for any condition of operation and amount of connected generating capacity, then that system or portion thereof is classified as solidly grounded. Such a system has the lowest overvoltages and protective level. If greater limitation of short-circuit current is desired, resistance or reactance may be introduced in the neutral. The permissible value of reactance lies in the range from $X_0/X_1=3$ to 10 for generator neutral grounding, although sometimes it may be higher for grounding transmission systems. The maximum permissible value of resistance for neutral grounding corresponds to the tuned reactance value of the ground-fault neutralizer. The isolated neutral system is poorest with respect to overvoltages and protective level.

STANDARDS • • •

AIEE Standards Manual Published

The standards committee of the Institute announces the publication of the "AIEE Standards Manual." The manual has been in course of development for several years, having been started by R. E. Hellmund during his chairmanship of the standards committee. His preliminary draft has been revised and developed by a subcommittee under H. R. Summerhayes (F '39). The pamphlet, it is hoped, will prove to be an effective guide to all committee chairmen and others engaged in the many phases of standards work now carried on by the AIEE. For many years it has been evident that there was a general lack of understanding of the machinery of standardization and the relationships existing particularly as to committees engaged in activities of joint interest to AIEE and the American Standards Association. The "AIEE Standards Manual" outlines in detail the various steps to be followed in standards work of such joint interest. Excerpts from the bylaws governing standards are given as well as the bylaws of the standards committee. Additional sections deal with the work of the various standards coordinating committees, basic guiding standards, test codes, electrical definitions, and publication procedure.

The standards committee urges that those engaged in standards work obtain a copy of this pamphlet, a careful reading of which should result in a much clearer understanding of the rather complex machinery which must be put in operation when new projects or revision of existing projects are undertaken. Copies may be obtained gratis from AIEE headquarters by writing H. E. Farrer, secretary of the standards committee.

Comments on Trial Standard Asked

AIEE Trial Standard 31, "Capacitance Potential Devices and Outdoor Coupling Capacitors," which was made available in December 1941 for trial use is to be revised and issued as a permanent standard. In its trial form it covers definitions, ratings, and accuracy performance characteristics of capacitance potential devices and outdoor coupling capacitors. Since sufficient time has elapsed to permit some use of the standard, the working group which developed it desires from interested engineers comments and suggestions which may be helpful in reshaping the standard for permanent use. All communications should be addressed to C. A. Woods, Jr., AIEE relay subcommittee of the committee on protective devices, switchgear engineering department, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

PERSONAL • • • •

O. A. Knopp (A '09, M '34) chief, bureau of tests and inspection, Pacific Gas and Electric Company, Emeryville, Calif., has recently retired. He was born August 24, 1877, in Berlin, Germany, and received his technical education in that country. His electrical engineering career in America began in 1901, when he accepted an invitation to introduce German technical methods to the students of the Blue Hill Observatory, Harvard University, Cambridge, Mass. Prior to this appointment he had been associated briefly with Bergmann Electrical Works, Berlin, Germany, as testing engineer, and as engineer at the Aeronautical Observatory of the Royal Prussian Meteorological Institute at Berlin. From 1902 to 1903 he was associated with General Electric Company in the testing department, learning the American methods of electrical engineering, and upon completion of the test course joined the Oakland Gas, Light and Heat Company as assistant superintendent. He became assistant superintendent of the Sacramento (Calif.) Gas and Electric Railway Company in 1904, and in 1905 he returned to Oakland (Calif.) Gas, Light and Heat Company, where he remained as superintendent of the meter and instrument department until 1908. From 1908 to 1915 he was superintendent of the standardizing laboratory and meter department, Pacific Gas and Electric Company, Oakland, Calif., and from 1915 to 1920 he served as superintendent of the laboratory department of the company's general office in San Francisco. In 1920 he was appointed chief, bureau of tests and inspection, a position he held until his retirement. He also is a member of the American Society for Testing Materials. In 1937 he was awarded the AIEE Pacific District Prize for initial paper for his paper "Some Applications of Instrument Transformers."

C. B. Joliffe (M '34) assistant to the president of the Radio Corporation of America and chief engineer, RCA Laboratories, New York, N. Y., has been appointed vice-president and chief engineer of the RCA Manufacturing Company, New York, N. Y. He has been associated with the RCA company since 1935. Doctor Joliffe was born November 13, 1894, in Mannington, W. Va., and received the degrees of bachelor of science (1915) and master of science (1920) from West Virginia University, where he served as instructor in physics from 1917 to 1918 and from 1919 to 1920. During World War I he served in the United States Army Signal Corps. From 1920 to 1922 he was instructor of physics at Cornell University, Ithaca, N. Y., and in 1922 he was awarded the degree of doctor of philosophy from that institution. From 1922 until 1930 he was associate physicist to the physicist, radio section, Bureau of Standards, Washington, D. C. He served as technical advisor for the International Consulting Committee on Radio at The Hague in 1929. In 1930 he was made chief engineer of the Federal Radio Commission, and when this was replaced in 1934 by the Federal Communications Committee, which is charged with regulation of all wire and radio communications, he became chief engineer of the new commission. In 1935 he was appointed engineer in charge of the RCA Frequency Bureau, Radio Corporation of America, later becoming chief engineer of the RCA Laboratories. He served on the AIEE committee on communication from 1934 to 1939 (chairman, 1936–37), and on the technical program committee for 1936–37. He is a fellow of the Institute of Radio Engineers and the American Association for the Advancement of Science, and a member of Phi Beta Kappa and Sigma Xi.

M. T. Crawford (A '07, F '22) assistant chief engineer, Puget Sound Power and Light Company, Seattle, Wash., has recently been appointed chief engineer. He has been associated with the company and its predecessors since 1906. He was born March 3, 1885, in Louisville, Ky., and received the degrees of bachelor of science in electrical engineering (1907) and a master's degree in electrical engineering (1910) from the University of Washington. He joined the Seattle-Tacoma Power Company in 1906 as draftsman and record clerk, becoming general foreman in charge of construction (1909), and assistant engineer (1910). In 1912 he was appointed superintendent of the transmission division, water power department, Puget Sound Traction Light and Power Company, when the Seattle-Tacoma Power Company consolidated with several other utilities. During this year he also acted as engineer for the Key City Light and Power Company, Port Townsend, Wash. He held successively the positions of superintendent of the transmission and distribution divisions, light and power department (1916–18) and acting general superintendent of the light and power department (1917). In 1919, when the company was reorganized, he became superintendent, distribution department, Puget Sound Power and Light Company, Seattle, Wash. He was appointed to his latest position in 1935. He is a member of Tau Beta Pi and Sigma Xi.

W. M. White (M '21) manager and chief engineer, hydraulic department, Allis-Chalmers Manufacturing Company, Milwaukee, Wis., retired recently. His retirement concludes a 31-year association with the company. He was born November 20, 1871, at Valley Head, Ala., and was graduated from Tulane University with the degree of bachelor of science in electrical engineering in 1899. He began his electrical engineering career in 1899 as assistant engineer, Drainage Commission of New Orleans, La., and from 1899 to 1901 he was in charge of the steam generating station, transmission lines, and the motor-driven pumping stations. From 1901 to 1911 he was associated with I. P. Morris Company, Philadelphia, Pa., as hydraulic engineer, and since 1911 he has held his present position with the Allis-Chalmers company. In 1930 he was awarded the honorary degree of doctor of science by Tulane University, and in 1940 he was named a Modern Pioneer by the National Association of Manufacturers. Doctor White is the holder of numerous patents and the author of several technical articles and papers. He served as a member of the AIEE committee on power generation from 1924 to 1929. He is also a member of the American Society of Mechanical Engineers, the American Society of Civil Engineers, the Franklin Institute, Sigma Xi, and Tau Beta Pi.

Gordon Thompson (A '12, M '18) assistant chief engineer, Electrical Testing Laboratories, Inc., New York, N. Y., has recently been appointed chief engineer. He was born in Charlestown, N. Y., on October 18, 1888, and received the degrees of bachelor of engineering (1909) and electrical engineer (1913) from Cooper Union. He entered the employ of Electrical Testing Laboratories in 1909, and was made foreman of the electrical laboratory in 1910. In 1913 he became engineer in charge of the electrical laboratory, serving in that capacity until 1919. During the years 1911–15 he also taught physics and electrical engineering courses at Cooper Union. From 1919–26 he was professor of electrical engineering at Nanyang University, Shanghai. He also served as consulting engineer for the Mission Architects Bureau of China, and designed illumination and electrical distribution apparatus in several public buildings in Changsa and Wuhu. He returned to Electrical Testing Laboratories in 1927 as assistant chief engineer. He has served on the AIEE standards committee since 1939, and the committee on domestic and commercial applications since 1941. He is also a member of the American Society for Testing Materials.

H. F. Boe (M '30) manager, district manufacture and repair department, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., has been elected vice-president of the company. He was born May 12, 1884, at Mansfield, Ohio, and first joined the Westinghouse company in 1903 in the testing department. Until 1910 he worked on dynamo and motor testing, ultimately becoming foreman in charge of testing in the department. In 1911 he was transferred to general engineering, and in 1913 he was appointed engineer in the sales engineer department. He left the company in 1916 for a brief association with the Vaile Kimes Company, Dayton, Ohio (1916–17), returning to the Westinghouse company's office in Rochester, N. Y., as sales engineer in 1918. In 1922 he was transferred to the Buffalo, N. Y., office as manager of the industrial division, and in 1925 he became manager of the Buffalo district. He was appointed manager of the eastern district, New York, N. Y., in 1935, commercial manager, East Pittsburgh, Pa., in 1937, and in 1938 he was appointed manager of the district manufacture and repair department. He received the Westinghouse Order of Merit in 1941.

K. R. Van Tassel (A'27) manager of sales, Lynn Motor Section, motor division, River Works, General Electric Company, Lynn, Mass., has been appointed manager of sales of the company's new integral-horsepower motor section of the motor division. He has been associated with General Electric Company since 1925, the year in which he received the degree of bachelor of science from Massachusetts Institute of Technology. After one year in the student engineering course in Schenectady, N. Y., he was transferred in 1926 to the transformer engineering department, Pittsfield, Mass. In 1928 he was transferred to the single-phase motor department, Pittsfield, ultimately becoming designing engineer for the department. In 1932 he was sent to the fractional-horsepower motor engineering department, Fort Wayne, Ind., later becoming a commercial engineer in this department, and finally, staff assistant to the manager of the Fort Wayne Works. He became manager of sales at the River Works in 1940.

H. V. Putman (A '23, M '32) manager, transformer division, Westinghouse Electric and Manufacturing Company, Sharon, Pa., has been elected a vice-president. He has been associated with the company continuously since 1925. He was born in Barker, N. Y., on May 27, 1899, and received the degrees of bachelor of science in electrical engineering (1920), master of science in electrical engineering (1921), and doctor of philosophy (1923) from Union College. Following a two-year association (1923–25) with Ideal Electric and Manufacturing Company, Mansfield, Ohio, first as design engineer and later as assistant chief engineer, he entered the employ of the Westinghouse company as design engineer on synchronous motors. He was appointed section engineer in the synchronous motor section (1928), assistant manager of the transformer engineering department (1929), manager of the transformer engineering department (1931), and was named manager of transformer division in 1941.

J. F. Wiggert (A '09, M '16) chief electrical engineer, Homestake Mining Company, Lead, S. D., retired recently. Following a five-year association with Edison Light and Power Company, La Crosse, Wis., and brief affiliations with various other companies, he entered the employ of the Homestake company in 1902 as assistant to the chief electrical engineer. He was associated with the development of electrical equipment at the Homestake Mining Company, almost from its beginning, including supervision of the development of the carrier-current system of shaft signalling. In 1923 he was appointed chief electrical engineer, a position he held until his retirement. He is also a member of the American Institute of Mining and Metallurgial Engineers, and a consulting engineer with the United States Bureau of Mines. He was born in La Crosse, Wis., on September 22, 1877.

C. L. Gust (A '15) assistant chief electrical engineer, Homestake Mining Company, Lead, S. D., has been appointed chief electrical engineer. He has been associated with the company since 1912, and has served as assistant chief electrical engineer since 1923. Prior to his appointment to that position he served in various other capacities, including operator at the Lead, S. D., substation and the Spearfish, S. D., hydroelectric power station, chief engineer at the Englewood, S. D., power station, chief operator of the Lead, S. D., substation, and foreman of electrical installation at the Lead, S. D., steam-turbo-generator station. He was born July 21, 1894, at Yankton, S. D., and received his first engineering experience from 1910 to 1912 with Stone and Webster Engineering Corporation, Everett, Wash., and Oregon Washington Water Power Company, Medford.

J. R. Read (A '04) president, Canadian Westinghouse Company, Ltd., Hamilton, Ont., has been appointed president of the Atlas Plant Extension, Ltd., Welland, Ont. He was born in Virginia, on October 24, 1879. He began his electrical engineering experience with Sterling White Lead Company, Parnassus, Pa., afterward spending three years as student engineer with Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa. He was employed as engineer by the Cherokee Gold Dredging Company, and the Northern California Power Company before entering the employ of Canadian Westinghouse in 1904 as sales engineer for the Vancouver (B. C.) district office. He was appointed Vancouver district manager in 1907, company vice-president in 1937, director in 1938, and president in 1939.

A. M. Unger (A '40) development engineer, Pullman Standard Car Manufacturing Company, Chicago, Ill., was the recipient of the $3,700 first award in the $200,000 Progress Program of the James F. Lincoln Arc Welding Foundation. His paper, written in collaboration with J. E. Chandlin, Jr., discussed the major considerations in the gradually increasing applications of welding to railroad passenger cars since 1936. **J. F. Petrowsky** (A '17, M '20) designing engineer, Burke Electric Company, Erie, Pa., was awarded $150 for his paper entered in the classification of functional machinery, and **L. J. Bentley** (A '40) design leader of maintenance department, Chevrolet Gear and Axle, Detroit, Mich., received a $100 award in the same classification.

F. M. Farmer (A '02, F '13) vice-president, and until recently, chief engineer of Electrical Testing Laboratories, Inc., New York, N. Y., has been appointed consulting engineer. He has been associated with Electrical Testing Laboratories since 1903, when he joined the staff as technical assistant. He became engineer for the company in 1906, chief engineer in 1912, and vice-president in 1929. He has been serving the United States government lately in the capacity of co-ordinator of inspection for the New York Ordnance district. He was a member of the AIEE board of directors (1934–38), and is a past vice-president (1938–39) and past president (1939–40) of the Institute.

W. D. Coolidge (A '10, M '34) vice-president and director of research, General Electric Company, Schenectady, N. Y., was awarded the honorary degree of doctor of laws by Ursinus College in recognition of his x-ray and other research work. The recipient of the AIEE Edison Medal in 1927, and the Washington Award in 1932, he received the degrees of bachelor of science in electrical engineering (1896) from Massachusetts Institute of Technology, and doctor of philosophy (1899) from the University of Leipzig.

J. W. E. Griemsmann (A '39) research laboratories, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., has recently been appointed to the staff of the graduate electrical engineering department of the Polytechnic Institute of Brooklyn, N. Y., as a research and teaching assistant. He received the degrees of bachelor of electrical engineering (1936) and master of electrical engineering (1938) from that institution.

R. C. Muir (A '08, F '36) vice-president in charge of engineering, General Electric Company, Schenectady, N. Y., has been granted the honorary degree of doctor of engineering by Manhattan College. He was graduated from the University of Wisconsin in 1905 with the degree of bachelor of science in electrical engineering, and was awarded the honorary degree of doctor of engineering from that institution in 1939.

P. S. Mancini (A '37) assistant engineer, Public Service Engineer's Office, Providence, R. I., has become public service engineer for that city. He was graduated from Massachusetts Institute of Technology in 1926 with the degree of bachelor of science, and has been associated with the public service engineering department of Providence since 1931.

W. L. Winter (A '21) sales engineer, Westinghouse Electric and Manufacturing Company, Salt Lake City, Utah, has been appointed sales engineer, central station and transportation divisions, San Francisco, Calif. He entered the employ of the Westinghouse company in 1916, and has worked in the Salt Lake City office since 1933.

A. R. Stevenson, Jr. (A '20, F '37) staff assistant to the vice-president in charge of engineering, General Electric Company, Schenectady, N. Y., has been elected to serve as a manager of the American Society of Mechanical Engineers.

L. A. Goalby (A '40) central station division, Westinghouse Electric and Manufacturing Company, Denver, Colo., has been appointed manager of the Denver office. He has served as apparatus sales engineer in this office since 1936.

G. T. Shoemaker (M '20, F '39) vice-president, Kansas City (Mo.) Power and Light Company, has been nominated vice-president of the American Society of Mechanical Engineers for 1942–43.

OBITUARY • • • •

John Harisberger (A '02, M '10) retired general superintendent and manager, division of power supply, Puget Sound Power and Light Company, Seattle, Wash., died on September 9, 1942. He was born December 2, 1871, in Wasen, Switzerland, and became a naturalized citizen of the United States in 1904. He first became identified with the electrical industry in 1892, when he was employed by the Northwest Thomson-Houston Electric Company and Northwest General Electric Company, Portland, Oreg., as shop apprentice and construction worker. From 1894 to 1895 he was employed by the Union Power Company, Portland, Oreg., as repair man and station operator, and from 1896 to 1898 he was associated with Portland (Oreg.) General Electric Company in electrical construction. In 1899 he was placed in charge of construction of the hydroelectric plant at Snoqualmie Falls, and upon its completion he became general superintendent of the Snoqualmie Falls (Wash.) Power Company. In 1903 he was appointed in addition general superintendent and electrical engineer for the Seattle (Wash.) Cataract Company and the Tacoma (Wash.) Cataract Company, retaining his position when the three companies combined to become the Seattle-Tacoma Power Company. In 1912, upon consolidation of the electric companies in the Puget Sound district, including the Seattle Electric Company, Seattle-Tacoma Power Company, Pacific Coast Power Company, and Puget Sound Power Company, he was appointed superintendent of water power in charge of all hydroelectric stations of the Puget Sound Traction, Light and Power Company, Seattle, Wash., which assumed its present name in 1919. In 1931 he was appointed general superintendent and manager of the division of power supply, in charge of all hydroelectric and steam-power-producing facilities and trunk transmission lines, a position he held until his retirement in 1938. From 1905 to 1911, Mr. Harisberger was a lecturer on the transmission and generation of electrical energy at the University of Washington, Seattle. He also served on the AIEE committee on power stations from 1916 to 1917, and from 1923 to 1924.

Frank Reith Phillips (M '27) chairman of the board of directors and the executive committee and former president of the Philadelphia (Pa.) Company and subsidiary companies, Pittsburgh, Pa., died October 23, 1942. He was born on October 29, 1876, in Cleveland, Ohio, and studied engineering by private tutor and at the Case School of Applied Science. In 1894 he joined the engineering department of the Cleveland (Ohio) City Railway Company, and in 1903, when the company was consolidated with the Cleveland (Ohio) Electric Company, he was appointed assistant master mechanic. From 1904 to 1906 he was master mechanic with the Cincinnati, Newport, and Covington Light and Traction Company, and in the latter year he was appointed chief engineer of the Michigan United Railways Company. After brief associations with the Ohio Brass Company, Mansfield, and the United Light and Traction Company, Mich., he entered the employ of the Pittsburgh (Pa.) Railways Company, a subsidiary of the Philadelphia Company, in 1910. He served successively as superintendent of equipment and acting general manager, and was ultimately appointed mechanical and electrical engineer. In 1926 he was appointed vice-president and general manager of the Duquesne (Pa.) Light Company, a Philadelphia Company subsidiary, and retaining this office, in 1928 became vice-president of the Equitable Gas Company, another subsidiary. He was named senior vice-president of the Philadelphia Company in 1929, and later in the same year was appointed president of all the subsidiaries of that company, including the Pittsburgh Motor Coach Company, the Alleghany County Steam Heating Company, and the Equitable Sales Company. In 1931 he became president and director of the Philadelphia Company as well as of the subsidiaries, a position he held until his recent resignation. He was active in the design of the first efficient double-deck motor cars, Pittsburgh, 1917, and originator of the low floor street car, involving the principle of small diameter wheels, since largely adopted by street-railway manufacturers and automobile designers.

Lester William Wallace Morrow (A '13, M '17, F '25) professor of engineering administration, Rutgers University, New Brunswick, N. J., died November 15, 1942. He was born August 7, 1888, in Hammond, W. Va., and received the degree of mechanical engineer from Cornell University in 1911. After a year as instructor in electrical engineering at Cornell University, Ithaca, N. Y., he became assistant professor of electrical engineering at the University of Oklahoma, Norman, in 1913. He became associate professor of electrical engineering in 1916, and in 1917 was made professor and director of the school of electrical engineering. During 1918–19 he was assistant director of the United States Army Signal Officer's School at Yale University, New Haven, Conn., and from 1918 to 1922 he was assistant professor of electrical engineering, Yale University, acting as senior professor and executive of the electrical engineering department. In 1922 Mr. Morrow joined the McGraw-Hill Publishing Company, Inc., New York, N. Y., as associate editor of *Electrical World*, becoming managing editor (1924) and editor (1928). In 1936 he resigned to become general manager, fiber products division, Corning (N. Y.) Glass Works, and in 1939 he returned to the McGraw-Hill Publishing Company as regional editorial director, Chicago, Ill. He is the author of a book on electric power stations, and numerous technical articles. He has served as chairman of the AIEE technical program (1923–25), and Edison Medal (1938–41) committees, and as chairman of the committees on the award of Institute prizes (1923–25), co-ordination of Institute activities (1934–37), consitution and bylaws (1937–39). He was serving currently as a member of the board of examiners and of the Edison Medal committee. He was also a member of Sigma Xi, American Society of Mechanical Engineers, American Electrochemical Society, and a grand counselor of Sigma Tau.

Joseph Franklin Porter (A '87, M '87, F '33) chairman of the board of directors, Kansas City (Mo.) Power and Light Company, died on November 7, 1942. He was born on June 27, 1863, in Harrison County, Iowa, and was graduated from Iowa State College in 1884 with the degree of bachelor of civil engineering. He began his career in electrical engineering in 1885 with the Des Moines (Iowa) Edison Light Company, and worked briefly in various capacities for the Appleton (Wis.) Edison Light Company, the Western Edison Light Company, Chicago, Ill., and the Abilene (Tex.) Water and Light Company. In 1887 he joined the Western Electric Construction Company, St. Louis, Mo., as foreman, and later in the same year established his own business as construction contractor. In 1889 he sold out to Edison United Manufacturing Company, New York, N. Y., and joined that company's New York office, remaining until 1891. From 1891 to 1892 he was associated in street railway supply and electrical equipment business in New York, N. Y., and in Pittsburgh, Pa., and in 1892, with J. G. White, he undertook a contract for the equipment of the Kansas City (Mo.) Elevated Railway Company. From 1893 to 1906 he was president of the Alton (Ill.) Railway, Gas, and Electric Company, and from 1906 to 1917 he was president of the Tri-Cities Railway and Light Company, of Davenport, Rock Island, and Moline, Ill. He served concurrently as chairman of the executive committee of United Light and Railways and president of the Cedar Rapids and Marion (Iowa) City Railway Company from 1912 to 1915, and was appointed president of the Kansas City Power and Light Company in 1917. He became chairman of the board of directors in 1939. He was a member of Tau Beta Pi and the American Society of Mechanical Engineers.

Frederick Walter Hadley (A '96, M '01) retired safety director, Georgia Power Company, Atlanta, died on June 7, 1942. He was born on July 13, 1871, in Winterset, Iowa, and was graduated from Massachusetts Institute of Technology in 1893 with the degree of electrical engineer. He immediately entered the employ of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., in general shop work and electrical testing, and in 1895 he was transferred to the design of electrical apparatus. He was associated with the West End Street Railway Power Plants, Boston, Mass., from 1896 to 1898, and in the latter year he entered the employ of Westinghouse Church Kerr and Company, Engineers, Boston, Mass., first as draftsman on plans for mechanical equipment and later as inspecting engineer on the erection and installation of electrical equipment for the South Union Station terminal, Boston, Mass. In 1899 he entered the employ of the Boston (Mass.) Terminal Company as assistant superintendent of the steam power plant, becoming superintendent in 1900. He returned to Westinghouse Church Kerr and Company, Engineers, in 1901, working in their New York office from 1901 to 1904. From 1904 to 1912 he was superintendent of the hydroelectric plant, Atlanta (Ga.) Water and Electric Company, and from 1912 to 1927 was superintendent of the hydroelectric plants, Georgia Railway and Power Company. In 1928 he was appointed to the safety and welfare department, Georgia Power Company, Atlanta, as safety director, a position he held until his retirement in 1931.

Herbert Benjamin Coho (A '94) proprietor, Coho Chemical Company, New York, N. Y., died on May 19, 1942. He was born on August 21, 1869, in Ashland, Pa., and began his engineering career in 1890 as a member of the Edison student course, Schenectady, N. Y. From 1891 to 1892 he was successively in charge of the company's Sprague tests and in service in the armature department and wiring tests. He became associated with the Wardell Engineering Company, Bridgeport, Conn., from 1893 to 1895, assisting in the installation of copper-zinc storage-battery systems. In 1895 he established the H. B. Coho Company, and until 1909 independently installed electrical plants in New York, N. Y. He also constructed a water-power development in Sullivan County, N. Y., during this period. From 1909 to 1915 he served as research engineer and assistant secretary for the United Lead Company, becoming, in the latter year, personnel director of the United States Cartridge Company, Lowell, Mass. During World War I he served in the United States Quartermaster Corps. In 1919 he became secretary of the New York Lumber Trade Association, remaining in this capacity and secretary of the New York Lumber Trade Association Club until 1932. In 1933 he founded the Coho Chemical Company, New York, N. Y., and was head of the company until his death.

Nelson Jarvis Darling (M '34) manager of the West Lynn and River Works, General Electric Company, Lynn, Mass., died October 26, 1942. He was born on August 7, 1884, in Toronto, Ont., and was graduated from Cornell University in 1907 with the degree of mechanical and electrical engineer. From 1907 to 1909 he was associated with the General Electric Company, Schenectady, N. Y., as student engineer in the test course, and in the latter year spent six months in the Panama Canal Zone on loan to the Dodge and Day Company, to assist in the installation of electrical apparatus. He returned to the General Electric Company to serve in the railroad engineering department at Schenectady from 1909 until 1915, when he was placed in charge of the mechanical and electrical engineering work at the General Electric plant at Erie, Pa. In 1918 he was made assistant works manager at Erie, Pa., and in 1922 he was appointed manager of the River Works, West Lynn, Mass. In 1935 he was named manager of the West Lynn Works in addition. During World War I he was engaged in the designing of machines for the manufacture of turbines and projectiles. He was a member of the AIEE committee on education from 1926 to 1928.

John Harcourt Williams (M '13) deputy chief electrical engineer, Manchester Corporation Electricity Department, England, died in July 1942. He was born on November 21, 1876, in Croydon, England, and received his technical education at the City and Guilds College, London, and as a student engineer from 1897 to 1899 with Messrs. Maudsley, Sons and Field, Marine Engineers, Lambeth, London. In 1900 he began a 30-year affiliation with the British Thomson-Houston Company, Ltd., Rugby, London, serving on the company's outside construction staff in Hull, Huddersfield, and Nottingham, until 1912. In that year he was appointed to the London staff, where he remained in charge of all construction work for the British Thomson-Houston Company in the London district until 1920. In 1920 he was transferred to Manchester as district manager of the North-West England district, and in 1930 he left the company to become deputy chief engineer to the Manchester Corporation Electricity Department. This position he held until his death in July, only three months before he was to have retired. He was also a member of the Institution of Electrical Engineers and the Institution of Civil Engineers, England.

Frank Eugene Danford (A '37) engineering department, General Electric Company, Schenectady, N. Y., died October 11, 1942. He was born on January 13, 1916, in Hutchinson, Kansas, and was graduated from Kansas State College in 1936 with the degree of bachelor of science in electrical engineering. He immediately entered the employ of the Kansas Power and Light Company, Hutchinson, as meter tester in the service engineering department. In 1937 he joined General Electric Company, Erie, Pa., as student engineer in the testing department. After a brief period in the Erie Works he was transferred to the engineering department of General Electric in Schenectady, N. Y., where he remained until his death. He had served for several years on the executive committee of the AIEE Schenectady Section.

MEMBERSHIP • •

Recommended for Transfer

The board of examiners, at its meeting on November 19, 1942, recommended the following members for transfer to the grade of membership indicated. Any objection to these transfers should be filed at once with the national secretary.

To Grade of Fellow

Battey, W. R., chief electrical engineer, Basic Magnesium, Inc., Las Vegas, Nev.
Forman, A. H., professor and head of department of electrical engineering, West Virginia University, Morgantown, W. Va.
Lindvall, F. C., professor of electrical engineering, California Institute of Technology, Pasadena, Calif.
Morrison, J. W., vice-president and chief engineer, Rochester Telephone Corporation, Rochester, N. Y.

4 to grade of Fellow

To Grade of Member

Ahlquist, R. W., teaching, Iowa State College, Ames, Iowa.
Amador, F. J., electrical engineer, The Panama Canal, Diablo Heights, Canal Zone.
Anderson, H. L., engineer, Commonwealth and Southern Corporation, Jackson, Mich.
Archer, L. B., assistant professor of electrical engineering, University of Illinois, Urbana, Ill.
Blakely, R. T. C., assistant engineer, International Business Machines Corporation, New York.
Burns, L. L., engineer, Southwestern Bell Telephone Company, Dallas, Texas.
Campbell, G. H., sales engineer, General Electric Company, New York.
Hegensick, district sales and service engineer, The Pyle National Company, St. Paul, Minn.
Hanstein, H. B., teaching electrical engineering, College of the City of New York, New York.
Harrap, G. V., technical assistant, West Ham Corporation, London, England.
Hiegel, A. J., design engineer, General Electric Company, Fort Wayne, Ind.
Holladay, C. H., treasurer, Los Angeles Railway Corporation, Los Angeles, Calif.
Mathis, J. D., Southwestern Bell Telephone Company, Dallas, Texas.
Milligan, L. S., assistant electrical engineer, George G. Sharp, New York.
Parker, W. A., lieutenant, United States Navy, Galveston, Texas.
Quistorff, W. W., assistant superintendent of distribution, Puget Sound Power and Light Company, Renton, Wash.
Raddin, E. H., chief engineer, Champion Lamp Works, Lynn, Mass.
Ratz, E. G., electrical engineer, Canadian Westinghouse Company, Hamilton, Ont., Canada.
Reich, H. J., professor of electrical engineering, University of Illinois, Urbana, Ill.
Thoms, E. G., plant extension engineer, Indiana Bell Telephone Company, Indianapolis, Ind.

20 to grade of Member

Applications for Election

Applications have been received at headquarters from the following candidates for election to membership in the Institute. Names of applicants in the United States and Canada are arranged by geographical District. If the applicant has applied for direct admission to a grade higher than Associate, the grade follows immediately after the name. Any member objecting to the election of any of these candidates should so inform the national secretary before December 31, 1942, or February 28, 1943, if the applicant resides outside of the United States or Canada.

United States and Canada

1. North Eastern

Erben, H. V. (Member), General Electric Company, Schenectady, N. Y.
Fogarty, J. B., United States Torpedo Station, Newport, R. I.
Hydrick, J. C., Jr., General Electric Company, Lynn, Mass.
Kelley, J. B., General Electric Company, Schenectady, N. Y.
MacCrehan, W. A., Jr., General Electric Company, Lynn, Mass.
Nelson, P. H., University of Connecticut, Storrs, Conn.
Ruggles, J. S., New York Power and Light Corporation, Troy, N. Y.
Schlotterbeck, R. S., General Electric Company, West Lynn, Mass.
Seder, L. A., General Electric Company, Lynn, Mass.
Stroberg, E. A. (Member), Stone and Webster Engineering Corporation, Boston, Mass.
Thomson, E. C., Photoswitch Incorporated, Lawrence, Mass.
Tracy, C. A., Westcott and Mapes, Incorporated, New Haven, Conn.
Vindsberg, S. B., Stone and Webster Engineering Corporation, Boston, Mass.

2. Middle Eastern

Akers, M. K. (Member), George Washington University, Washington, D. C.
Bean, T. H., Leeds and Northrup Company, Philadelphia, Pa.
Brennan, F. E., Consolidated Gas and Electric Company, Baltimore, Md.
Crawford, J. O., Bell Telephone Company, Ardmore, Pa.
Danser, J. W., United States Naval Academy, Annapolis, Md.
Crevensten, H. A., Baltimore Transit Company, Baltimore, Md.
Dodd, W. E., Line Material Company, Zanesville, Ohio.
Gard, J. D., Westinghouse Electric and Manufacturing Company, E. Pittsburgh, Pa.
Gilley, J. L., Consolidated Gas and Electric Light and Power Company, Baltimore, Md.
Hartley, R. A., Westinghouse Electric and Manufacturing Company, E. Pittsburgh, Pa.
Hawkeswood, A. E., British Admiralty Delegation, Washington, D. C.
Hecklinger, R. S., Consolidated Gas and Electric Company, Baltimore, Md.
Horstman, W. F., General Electric Company, York, Pa.
Hudak, J. L., Ohio Injector Company, Wadsworth, Ohio.
Klein, G. G., Baltimore Transit Company, Baltimore, Md.
Marsh, R. A., New York Shipbuilding Corporation, Camden, N. J.
Mayer, W. S., Jr., United States Signal Corps, Philadelphia Signal Depot, Philadelphia, Pa.
Moses, G. L. (Associate re-election), Westinghouse Electric and Manufacturing Company, E. Pittsburgh, Pa.
Oughton, J. R., III, Line Material Company, Zanesville, Ohio.
Payne, J. F., Jr., Leeds and Northrup Company, Philadelphia, Pa.
Ripley, C. S. (Associate re-election), 3030 Euclid Avenue, Cleveland, Ohio.
Smith, W. L., The Electric Controller and Manufacturing Company, Cleveland, Ohio.
Soebbing, T. J., Philadelphia Signal Corps, Procurement District, Philadelphia, Pa.
Tankovich, J. G. (Associate re-election), Line Material Company, Zanesville, Ohio.
Towhey, H. M., Westinghouse Electric and Manufacturing Company, Philadelphia, Pa.
Triche, R. P., Westinghouse Electric and Manufacturing Company, Sharon, Pa.
Vandeventer, D., Leeds and Northrup Company, Philadelphia, Pa.
Von Alven, W. H., Capitol Radio Engineering Institute, Washington, D. C.
Wadlow, F. T. (Member), Prack and Prack and Chester Engineers, Pittsburgh, Pa.
Weinfield, B. S., General Electric Company, Philadelphia, Pa.
Woodward, L. H. L., New York Shipbuilding Corporation, Camden, N. J.
Zwelling, M. I., Line Material Company, Zanesville, Ohio.

3. New York City

Adashko, J. G. (Member), Gielow, Incorporated, New York, N. Y.
Becken, E. D., R.C.A. Communications, Incorporated, New York, N. Y.
Blomberg, K. H. (Member), Ericsson Telephone Sales Corporation, New York, N. Y.
Carver, J. V., Jersey Central Power and Light Company, Morristown, N. J.
Coe, W. H., Eclipse-Pioneer Division Bendix Aviation Corporation, Bendix, N. J.
Davis, E. R., Eclipse Aviation Division, Bendix, N. J.
De Marrais, R. J. (Member), Wright Aeronautical Corporation, Paterson, N. J.
Donaldson, W. H., Bendix Aviation Corporation, Bendix, N. J.
Einsiedler, C. S., K. W. Battery Company, Incorporated, Brooklyn, N. Y.
Flagg, H. J. (Member), General Cable Corporation, Perth Amboy, N. J.
Fort, A. C., Jersey Central Power and Light Company, Morristown, N. J.
Haines, W. T., Bell Telephone Laboratories, Incorporated, New York, N. Y.
Harris, J. R., Bell Telephone Laboratories, Incorporated, New York, N. Y.
Heggy, J. M. (Member), Westinghouse Electric and Manufacturing Company, Newark, N. J.
Hunter, W. (Member), Ebasco Services Incorporated, New York, N. Y.
Jorgensen, H., Postal Telegraph-Cable Company, New York, N. Y.
Kiimalehto, E., New York Telephone Company, New York, N. Y.
Kirk, D. (Member), The J. G. White Engineering Corporation, New York, N. Y.
Kirkpatrick, W. T., Kingston-Conley Electric Company, North Plainfield, N. J.
McAvoy, F. E., General Bronze Corporation, Long Island City, N. Y.
Miller, R. F., Gielow, Incorporated, New York, N. Y.
Nilan, J. J., Sr., Consolidated Telegraph and Electrical Subway Company, New York, N. Y.
Plotkin, J. (Associate re-election), Burns and Roe, Incorporated, New York, N. Y.
St. Andre, J. W. (Member), Wright Aeronautical, Plant #7, Paterson, N. J.
Schaefer, J. A., General Electric Company, Bloomfield, N. J.
Straussman, A. D., Long Island Lighting Company, Mineola, N. Y.
Van Auken, O. W., Jersey Central Power and Light Company, Morristown, N. J.
Van Hamel, T. A. (Member), The J. G. White Engineering Corporation, New York, N. Y.
Weiss, L., Federal Telephone and Radio Manufacturing Corporation, East Newark, N. J.
Welter, F. J., Consolidated Edison Company of New York Incorporated, East River Station, New York, N. Y.
Wendt, R. L., Sperry Gyroscope Company, Brooklyn, N. Y.

4. Southern

Blumburg, A. O., Alabama Dry Dock and Shipbuilding Company, Mobile, Ala.
Crisman, J. A., Westinghouse Electric and Manufacturing Company, Memphis, Tenn.
Drawe, W. J., Jr., New Orleans Public Service Incorporated, New Orleans, La.
Gwinner, H. A., American Telephone and Telegraph Company, Birmingham, Ala.
Hayes, A. C., Tampa Electric Company, Tampa, Fla.
Humbert, W. F. (Associate re-election), Marshall Field and Company, Spray, No. Car.
Kammer, K. P., New Orleans Public Service Incorporated, New Orleans, La.
Kittrell, L. W., Southwestern Gas and Electric Company, Shreveport, La.
Levy, M. L. (Member), Higgins Industries, Incorporated, New Orleans, La.
O'Clare, A. R. (Associate re-election), Miami School of Aeronautics, Miami, Fla.
Partain, W. L., Fraser-Brace Engineering Company, Incorporated, Hogansville, Ga.
Trouard, S. E. (Member), New Orleans Public Service Incorporated, New Orleans, La.
Wallisch, C. J. (Member), Tennessee Valley Authority, Knoxville, Tenn.

5. Great Lakes

Barnes, H. O., Sangamo Electric Company, Springfield, Ill.
Burnett, J. E., Westinghouse Electric and Manufacturing Company, Detroit, Mich.
Dumont, D. P. (Member), Indiana General Service Company, Marion, Ind.
Eckel, A., McGraw-Hill Publishing Company, Incorporated, Chicago, Ill.
Foscue, E. S., General Electric Company, Ft. Wayne, Ind.
Foster, S. L., Allis-Chalmers Manufacturing Company, Milwaukee, Wis.
Iglehart, M. M. (Member), Cutler-Hammer, Incorporated, Milwaukee, Wis.
Jacobs, H. S., Harnischfeger Corporation, Milwaukee, Wis.
Kasak, A. J. (Member), Bendix Aviation, South Bend, Ind.
Kober, C. F., Defoe Shipbuilding Company, Bay City, Mich.
Kuzela, J. F., Jr., Line Material Company, South Milwaukee, Wis.
Lithgow, E., Square D Company, Milwaukee, Wis.
Martin, J. S. (Member), Sangamo Electric Company, Springfield, Ill.
Osborn, M. W., Allis-Chalmers Manufacturing Company, Milwaukee, Wis.
Patti, F. D., The Austin Company, Midland, Mich.
Rook, W. E., American Telephone and Telegraph Company, Chicago, Ill.
Schaefer, M. W., Allis-Chalmers Manufacturing Company, West Allis, Wis.
Slaybaugh, E. O. (Member), Consumers Power Company, Battle Creek, Mich.
Swinehart, M. R., Cutler-Hammer, Incorporated, Wilwaukee, Wis.
Tucker, J. G. (Member), Commonwealth Edison Company, Chicago, Ill.
Vogel, R., Square D Company, Milwaukee, Wis.
Wrentmore, L. L., Westinghouse Electric and Manufacturing Company, Chicago, Ill.

6. North Central

Pollock, P. R., Allis-Chalmers Manufacturing Company, Denver, Colo.

7. South West

Acosta, R., Rural Electrification Administration, St. Louis, Mo.
Auten, R. S., Signal Corps, United States Army, San Antonio, Texas.
Berry, J. S., I. A. Bennett and Company, St. Louis, Mo.
Brandt, W. T., Sr., Pennsylvania Shipyards, Incorporated, Beaumont, Tex.
Carpenter, H. E., Oklahoma Gas and Electric Company, Oklahoma City, Okla.
Cerna, H., Rural Electrification Administration, St. Louis, Mo.
Church, R. A., University of Oklahoma, Norman, Okla.
Del Ponte, F., Rural Electrification Administration, St. Louis, Mo.
Freebersyser, R. W., Westinghouse Electric and Manufacturing Company, St. Louis, Miss.
Gamp, L. H., Jr. (Member), Gamp Electric Company, St. Louis, Mo.
Gil, M., Rural Electrification Administration, St. Louis, Mo.
Grace, W. J. (Member), 7 Branard Court North, Wilshire Village, Houston, Tex.
Harmon, R. C., U. S. Signal Corps, University of Kansas, Lawrence, Kan.
Ladd, R., Rural Electrification Administration, St. Louis, Mo.
Rigby, W. S., Wagner Electric Corporation, St. Louis, Mo.
Rivas, J., Rural Electrification Administration, St. Louis, Mo.
Romero, F., Rural Electrification Administration, St. Louis, Mo.
Schroeder, F. W., Sr. (Member), West Texas Utilities Company, Abilene, Tex.
Stueve, W. H. (Member), Oklahoma Gas and Electric Company, Oklahoma City, Okla.
Williams, H. J., Missouri Electric Power Company, Marshfield, Mo.

8. Pacific

Crosby, R. E., Bethlehem Steel Company, San Francisco, Calif.
Hendrickson, J. A., Pacific Electric Manufacturing Corporation, San Francisco, Calif.
Hong, K. S., Moore Dry Dock Company, Oakland, Calif.
Koller, R., Pacific Electric Manufacturing Corporation, San Francisco, Calif.
Meigs, J. R., University of Southern California, Los Angeles, Calif.
Rose, E. A., 1686 Scenic Avenue, Berkeley, Calif.
Williams, P. H., 2736 Center Street, Bakersfield, Calif.

9. North West

Flechsig, A. J. (Member), The Austin Company, Naval Air Station, Sand Point, Seattle, Wash.
McLean, K. J., General Electric Company, Seattle, Wash.
Stroman, R. E., Mountain States Telephone and Telegraph Company, Fort Peck, Mont.

10. Canada

MacKay, T. A., Hamilton Hydro-Electric System, Hamilton, Ont., Canada.
Squire, E. T., Canadian (British Columbia) Telephone and Supplies Limited, Vancouver, B. C., Canada.
Townson, C. H., Railway and Power Engineering Corporation, Toronto, Ont.

Total, United States and Canada, 145

Elsewhere

Cook, J. E. (Associate re-election), Cia Colombiana de Electricidad, Aptdo. Aereo 532, Santa Marta, Colombia, S. A.
Dreher, C. E., Westinghouse Electric Company of Brazil, Rio de Janeiro, Brazil, S. A.
Hournon, B. A., Westinghouse Electric International Company, Buenos Aires, Argentina, S. A.
Loewel, R. M., Cia Argentina de Electricidad, Buenos Aires, Argentina, S. A.

Total, elsewhere, 4

OF CURRENT INTEREST

War Problems Discussed at Tenth Annual Meeting of ECPD

War problems and the effects of the war on the work of the Engineers' Council for Professional Development held the spotlight at the tenth annual meeting of the Council held in New York, N. Y., October 18, 1942. Among important actions taken were the adoption of a plan of operation for carrying out ECPD functions locally, proposed by AIEE representative J. F. Fairman (F '25), vice-president, Consolidated Edison Company of New York (N. Y.), Inc., and outlined previously at the AIEE 1942 summer convention conference of officers, delegates, and members held in Chicago, Ill., in June 1942.

Canons of ethics for engineers, prepared by a special committee on principles of engineering ethics, were approved by the ECPD for submission to the boards of directors of the constituent societies for consideration and comment. It was announced also that a series of resolutions pertaining to the war man-power problem had been adopted by the ECPD executive committee at its meeting of September 20, 1942 (*EE, November '42, pp. 583–4*).

A new special ECPD committee on unionization of engineers was authorized to study the union problem as it relates to engineers and report at a future meeting. Upon recommendation by the committee on professional training, the publication of a "Manual for Junior Engineers," which has been under preparation during the past year by that committee, was authorized. Also reports were presented by a representative of each of the eight constituent societies of ECPD.

On recommendation of the committee on engineering schools, ECPD accreditation of curricula was granted as follows:

University of Notre Dame: aeronautical, civil, electrical, mechanical, and metallurgical engineering.

University of Toledo: course in general engineering.

Bucknell University: chemical engineering.

Catholic University of America: mechanical engineering.

Cooper Union Night School of Engineering: chemical engineering.

University of Florida: chemical engineering.

Georgia School of Technology: ceramic engineering.

University of Idaho: geological engineering.

University of Maryland: chemical engineering.

Northeastern University: chemical engineering.

Oregon State College: chemical engineering.

Texas Agricultural and Mechanical College: aeronautical engineering.

University of Southern California: civil, electrical, and mechanical engineering.

Worcester Polytechnic Institute: chemical engineering.

A total of 577 engineering curricula in 131 colleges and universities in the United States have been accredited by ECPD through the committee's inspection program during the last decade. Only degree-granting colleges are eligible to be accredited by ECPD.

LOCAL ACTIVITIES TO DEVELOP ECPD AIMS

The plan, promulgated by J. F. Fairman, for carrying out locally the purposes of ECPD provides for the establishment of programs in local sections or chapters of constituent societies to bring about an understanding of ECPD objectives and an active interest in them (*EE, August '42, pp. 419–21*). Although many have thought that ECPD's most significant accomplishment has been its accrediting work, the organization has other aims, no less significant, toward which its work must be shaped—selection and guidance, professional training, and professional recognition.

It is suggested that local sections or chapters of member societies might best become active in promoting work on selection and guidance patterned along the lines of procedures already developed in certain metropolitan areas in connection with the work of the ECPD committee on student guidance. This committee endeavors to develop means for the educational and vocational orientation of young men as to the responsibilities of the engineer and opportunities in engineering, in order that only those who have the high-quality aptitude and capacity required of engineers seek to enter the profession. Co-operation with local high schools in conducting vocational counseling work with prospective students and in planning programs to stimulate interest of parents and students in engineering is urged.

In enlarging the scope of work in professional training aimed at assisting young graduates to become oriented and active in their own professional development, a manual designed for the guidance of the young engineer during his formal educational period and during the ensuing apprenticeship is now in progress. The ECPD committee on professional training also has arranged to make engineering periodicals available to members of constituent societies who are in the Armed Services by having the publications sent to regularly established libraries (*EE, July '42, p. 359*).

Attempts to encourage co-operation of local engineering groups with one another and with high-school and college officials in this program are emphasized inasmuch as ECPD itself is not organized to enter the local field. However, committees of ECPD will give assistance by answering questions, by supplying information as to the experiences of other local groups, and by making suggestions based on their observations.

Officers and Committees

ECPD officers elected for 1942–43 are:

R. E. Doherty, president of Carnegie Institute of Technology, Pittsburgh, Pa., *chairman* (re-election); S. D. Kirkpatrick, editor, *Chemical and Metallurgical Engineering*, New York, N. Y., *vice-chairman;* A. B. Parsons, secretary, American Institute of Mining and Metallurgical Engineers, *secretary;* S. L. Tyler, secretary, American Institute of Chemical Engineers, New York, N. Y., *assistant secretary.*

Members of the executive committee are:

R. E. Doherty, chairman ex officio; S. D. Kirkpatrick, vice-chairman ex officio; W. B. Plank, professor of mining, Lafayette College, Easton, Pa., AIME; J. F. Fairman, assistant vice-president, Consolidated Edison Company of New York (N. Y.), Inc., AIEE; A. R. Stevenson, Jr., staff assistant to vice-president in charge of engineering, General Electric Company, Schenectady, N. Y., ASME; B. F. Dodge, professor and chairman of department of chemical engineering, Yale University, New Haven, Conn., AIChE; C. C. Williams, president, Lehigh University, Bethlehem, Pa., SPEE; J. B. Challies, vice-president and executive engineer, Shawinigan Water and Power Company, Montreal, Que., EIC; ASCE and NCSBEE to be announced.

The following representatives of ECPD member societies were announced for the 1942–45 term:

Reappointments—G. W. Burpee, consulting engineer, Coverdale and Colpitts, New York, N. Y., ASCE; R. L. Sackett, dean emeritus of engineering, Pennsylvania State College, ASME; O. W. Eshbach, dean Northwestern Technological Institute, Northwestern University, Evanston, Ill., AIEE; C. M. A. Stine, vice-president, E. I. duPont deNemours and Company, Wilmington, Del., AIChE; Arthur Surveyer, consulting engineer, Montreal, Que., EIC.

New appointments—A. F. Greaves-Walker, professor of ceramic engineering, North Carolina State College of Agriculture and Engineering, Raleigh, AIME; D. B. Prentice, president, Rose Polytechnic Institute, Terre Haute, Ind., SPEE.

Newly elected committee chairmen for 1942–43 are:

Committee on student selection and guidance, A. R. Cullimore, president, Newark (N. J.) College of Engineering.

Committee on information, E. H. Robie, editor, American Institute of Mining and Metallurgical Engineers.

The following committee chairmen were re-elected for the coming year:

Committee on engineering schools, D. B. Prentice, president, Rose Polytechnic Institute, Terre Haute, Ind.

Committee on professional training, E. S. Lee, engineer, general engineering laboratory, General Electric Company, Schenectady, N. Y.

Committee on professional recognition, C. F. Scott, professor emeritus of electrical engineering, Yale University, New Haven, Conn.

Report of Chairman Doherty

"During the current year ECPD has not escaped the pressures of war, and the prospects are that it will feel them still more," began Chairmen Robert E. Doherty in his annual report to ECPD. "However," he continued, "the work of the Council has gone forward with thoughtful and, I hope, wise adaptation to the war pressures. Despite the outlook immediately after Pearl Harbor that ECPD would probably be obliged to curtail greatly its several activities, the actual trend has been the opposite."

Concerning future activities of ECPD, Chairman Doherty expressed the view that "the Council should take an aggressive position. It should pursue at an accelerated rate every activity within its charter that gives promise of supporting the war effort or that would lay foundations of professional development that may now appear to be essential for the most effective service to the country by engineers and the engineering profession in peace-time reconstruction. And as I now briefly scan the several areas of the ECPD landscape I shall comment specifically where I think especial emphasis should be laid, and this procedure will indicate more definitely the policy I would recommend.

"In the first place I wish to say a word concerning the question raised by the following action of the executive committee at its June 8, 1942, meeting:

> **VOTED** that it is the consensus of the executive committee that publicity and other activities of ECPD be directed toward the professional development of the individual engineer for a greater effectiveness in dealing with social and economic problems, as well as with technical problems.

"Singularly enough the original purpose of this vote was to expand, not contract, the scope of activity to include professional development in social and economic, as well as in technical problems. This enlarged scope is authorized specifically in the charter. Discussion of the matter at the meeting brought out the consensus that in carrying out the enlarged purpose, activities should be directed toward the professional development of the individual engineer. . . ."

Referring to earlier years, and discussing methods by which ECPD might reach the individual, Chairman Doherty said in part: "There are two general purposes which I would recall. Two years ago it seemed clear that one of the ways in which ECPD could best promote professional development was to cultivate more fully among the constituent bodies an attitude of co-operation and an improved practical facility in taking joint action. There was of course no thought of joint action on matters outside the scope of ECPD, or even on untimely matters within the scope. Simply, co-operation in ECPD matters would presumably make it easier to co-operate in others. If so, an advance would be made in professional development. While there is here no dramatic advance to report, I am certainly not discouraged with the progress and wish to emphasize that, in my opinion, this end is no less important today than it was two years ago.

"The other purpose was an educational campaign. The objectives of ECPD could never be achieved if the members of the Council, or perhaps even only the members of the executive committee, knew what ECPD was and what it was trying to do. Individual engineers must know and be interested in these activities. Hence every effort was to be made to inform the individuals among the boards of direction and the memberships of the several constituent bodies—indeed all engineers—of the purposes and activities of ECPD and to encourage them to have an active interest. As you all probably know, substantial progress has been made in this connection, and here also I urge that the matter is no less important than it was two years ago.

"Machinery for reaching the individuals was missing, but this has been planned. Indeed its essential elements already existed; it needed only to be defined and put in motion. As to reaching the individual members of the boards of direction, the three representatives of each of the several constituent bodies were the potential, but inactive, liaison between the Council and the board members. This liaison has begun to be effective, and the reports of these delegations at the annual meeting represent the two-way flow of this plan. And, needless to say, the secretaries of the constituent bodies are most helpful in keeping this part of the machinery oiled.

"The problem of reaching individual engineers generally is somewhat more complicated. Fortunately, however, in this case also, most of the main elements of necessary machinery are in existence. The channels of flow from the Council to such individuals are the national organizations of the constituent bodies. In all major cities and industrial centers there are local branches or sections of the national organizations; and on the campuses of most engineering schools there are student branches, and on some of them, branches of the Society for the Promotion of Engineering Education also. There should of course be SPEE branches on all such campuses and a move to this end should be encouraged. It remains to organize at such centers where the organizations do not already exist joint groups in which activities relating to professional development can be centered, and which, through local branches or sections, can be in communication with ECPD through the national organizations. Such a plan, it seems, is essential to the Council's work, and Mr. Fairman and the chairman have undertaken the formulation of a detailed proposed plan. Here again, I would add, the sympathetic help of the several secretaries is absolutely essential."

Chairman Doherty concluded his report with a discussion of the financial situation of ECPD, which he characterized as sound, and the work of the ECPD standing committees. Excerpts from the reports of these four committees follow.

ECPD Committee Reports

STUDENT SELECTION AND GUIDANCE

A summary of ten years' work by the committee on student selection and guidance was presented by R. L. Sackett, dean emeritus of engineering, The Pennsylvania State College, State College. Dean Sackett retired from the chairmanship of this committee after having served for the entire ten-year period since ECPD was first organized. "A review of the reports submitted by this committee reveals that three fields of action have been attempted," Dean Sackett reported. "The first was exploration. We found education and psychology could give little light on the aptitudes which differentiated the engineer. The committee gave the co-operative test service examinations in mathematics and English to nearly 2,000 entering freshmen engineers. The conclusion that mathematics was one criterion of engineering ability, was clear. The English test showed a lower correlation but when analyzed into its parts of spelling, vocabulary, and usage, it was observed that usage of words was a better criterion than the result of the entire test."

Calling "guidance by engineers" the second effort of the committee, Dean Sackett outlined the early efforts in that direction and discussed the organization of advisory committees of engineers. "Such committees have been organized through the interest of individuals in city and state engineering societies and by appeal to local sections of national engineering societies," the report stated. "Whenever the need is impressed on local sections the response has been excellent but the most significant promotional work remains to be done with the local sections." The report also reviewed results achieved by advisory committees in various localities, and called attention to the "Manual for Committees or Engineers Who Aid Young Men Interested in Engineering Education and the Engineering Profession," which had been produced in printed form during the past year.

"The third enterprise and an important one," the report continued, "depended on the interest of President A. R. Cullimore of the Newark (N. J.) College of Engineering [and newly appointed chairman of the committee on student selection and guidance] in the objectives of this committee. Through him Doctor Russell S. Bartlett was loaned to the committee, and a corresponding committee of the SPEE, to carry on a study of tests and especially to apply a battery of tests designed to show whether the engineer was different or just run of mine in his interests and aptitudes. Dr. Bartlett was familiar with the extended psychological studies made under the direction of Dr. A. B. Crawford at Yale University for mental differences between arts, science, and engineering students.

"Arrangements were made to give such a battery to arts and engineering freshmen at the opening of institutions in the fall of 1941. The following participated: The Universities of Florida, Missouri, Tennessee, Texas, Northwestern University, and Pasadena Junior College. A set of six tests was administered to about 2,000 entering freshmen in arts and engineering.

"The results are embodied in a report to be published in the *Journal of Engineering Education* at an early date. It is too long to do more than summarize it here. The results indicate that differences are exposed by such tests. In general the engineering student who rates high in mathematics, spatial visualization, and mechanical aptitudes is a good prospect for engineering or science education. His scores may not be as high in verbal comprehension, artificial language, or verbal reasoning as the typical arts student who will normally stand lower in mathematics, spatial relations, and mechanical aptitude. As a matter of fact, the graphs or profiles of achievement in such a battery range all the way from those few students who make high scores in all subjects down to those who do so poorly in all six subjects that they indicate poor preparation for any of the usual academic fields.

"A slightly modified battery of tests of the same general content was given to about 1,000 high-school juniors resident in or near Newark, N. J. Of the results Doctor Bartlett says, 'Evidence that the tests are measures of aptitude is found in the comparable range of scores of the two groups, college and high school, in most cases two years apart in academic age.'

"If supported by further experiments, and the study should be continued, the report sets a definite milepost of advance in test programs. We have no evidence of a corresponding differential study having been made heretofore. It is significant as an achievement and would justify the efforts of the committee to make a contribution to the selection and guidance of engineering students."

ENGINEERING SCHOOLS

In the report of the ECPD committee on engineering schools, D. B. Prentice, president, Rose Polytechnic Institute, Terre Haute, Ind., chairman, reviewed the work of the committee during the past year and outlined the basic policies of the committee's accrediting program. "During the current year," the report stated, "visits were made to 8 institutions for the purpose of reinspecting 35 curricula on the accredited list, to 11 institutions with one or more accredited curricula to appraise 15 nonaccredited curricula, and to two institutions on the accredited list to examine six curricula." The status of the accrediting program as of October 1942 is indicated in Tables I and II.

The committee's plans during the war period call for curtailment of the accrediting program as indicated by the following paragraphs: "Many members of engineering-college faculties are carrying additional responsibilities during the war as consultants or teachers of Engineering, Science and Management Defense Training courses. Some engineering departments have temporarily lost important staff personnel to military service or research. Preparation for an inspection or reinspection by a delegatory committee of the committee on engineering schools is an obligation which in many instances it is possible and safe to postpone; also members of the delegatory committees often find it difficult to arrange for even short inspection trips.

"For the above reason the committee on engineering schools proposes to defer many of the visits to institutions for the examination of curricula which would normally be conducted during the next year. The existing accreditations, pending reinspections, will be continued. Exceptions to this plan will be made in cases which seem to regional chairmen to need attention or when postponement of an inspection would be unjust to a college.

Table I. Status of the Accrediting Program by Curricula

As of October 18, 1942, Not Including Options Under Accredited Curricula

Curricula	Totals
Fully accredited	495
Provisionally accredited	74
Not accredited	103
Action pending	9
Total	681

"The curtailment of the inspection program notwithstanding, the committee on engineering schools feels that strict attention should be given during the war years to the end that college educational standards are not allowed to slip or facilities run down.

"There are an ever increasing number of war measures that bring external pressure to bear on now war-tensed engineering faculties and student bodies. The expedited programs, the raids on faculties and student bodies, and the ESMDT are perhaps the most apparent. The committee believes that although it is too early to draw any definite conclusions, the situation merits continued attention and data should be accumulated to determine what action can be recommended in the future.

"One of the major problems of the war effort centers around the proper technical training for war production positions. The committee proposes that wherever in critical areas the War Manpower Commission or the Selective Service System requests its aid in determining the appropriateness of certain training programs as being 'training for essential industries' and therefore justifying military deferment, this committee is willing to assist through the formation of area or regional engineering war manpower committees."

PROFESSIONAL TRAINING

"The abilities during the past few years of the ECPD committee on professional training have been applied to surveying the present opportunities for the professional training of the younger engineers," stated the report of this committee. Everett S. Lee (F '30), engineer, general engineering laboratory, General Electric Company, Schenectady, N. Y., is chairman of the main committee, and John T. Sherman, American Cyanamid Company, New York, N. Y., is chairman of the junior committee. The preparation of a 'Manual for Junior Engineers' was reported to be the committee's principal activity during the past year. Progress on this project is indicated in the following paragraphs from the report.

"The recommendations of the committee on professional training last year stressed the desirability of a close co-ordination between the agencies involved in the professional training of the younger engineer. To this end the desirability of a 'Manual for Junior Engineers' has been suggested, and the start of the work involved in the writing of such a manual has been the major activity of the committee for this past year.

"In the past the generally basic thought has been that when the engineering student graduates he leaves school and goes into active practice, which is largely in industry, in teaching, or in other varied forms.

"The manual would rather progress the thought that in the undergraduate course the student is building a foundation for his later professional life to include both his technical and his social contributions, and thus he should undertake to lay out a program for himself to insure progressive improvement in a well-rounded life.

"To be successful this thought must be basic on the part of the student, on the part of industry, and on the part of the engineering schools—on the student that he recognize it and pursue it; on industry that the young engineer is made conscious of an interest in him by what he finds there; and on the schools that they plant the seed in the undergraduate days and inspire and provide for a continuance of opportunity for advancement in both specific and general knowledge. The student would thus be graduated with thoughts of continued self-improvement, and wherever he went, he would find a proper climate in which to raise the planted seed.

"The 'Manual for Junior Engineers' would bring to all an understanding of this mutually accepted thought—to the student, to industry, to the engineering schools—and would provide a tangible means for progressing it."

Table II. Status of the Accrediting Program by Institutions

As of October 18, 1942

Approximate number of engineering degree-granting institutions in the United States		167
Institutions inspected (not including reinspections)		143
Institutions having one or more curricula accredited	132	
Institutions inspected but having no curricula accredited	9	
Institutions reinspected but having no curricula accredited	2	
Total number of institutions acted upon	143	
Institutions applying but inspection deferred		5
Institutions that have not applied		19

Regarding the effect of the war on the committee's activities, the report concluded: "The war has naturally prevented the normal amount of time being given to the work of the committee. Several of the members have entered into the Service. But on the part of those remaining, there is the definite conviction that helpful service may be rendered in the future by such application as is possible towards the attainment of our stated objectives, and thus, all possible application is being made. The writing of the 'Manual for Junior Engineers' will not be an easy task, but such assembling of a proper statement for guidance of the junior engineer and his associates should be worth many times the effort."

PROFESSIONAL RECOGNITION

Early ambitions of the ECPD committee on professional recognition, of which Charles F. Scott (HM '29), professor of electrical engineering emeritus, Yale University, New Haven, Conn., is chairman were reviewed in the annual report of that committee. The report continued with a review of the collapse of the early program and of the committee's fresh start in 1938. Principal activities reported for the year 1941–42 center around means for actually making students aware of their profession. The means pursued during the year comprised the publication and dissemination of an address by William E. Wickenden, president, Case School of Applied Science, Cleveland, Ohio, entitled "The Second Mile." Doctor Wickenden's address was delivered originally at a meeting of the Engineering Institute of Canada and was subsequently published in slightly revised form in *Electrical Engineering* (*May '42, pp. 242–7*) and in other society publications. Under sponsorship of the ECPD committee on professional recognition, the address was reprinted from *Electrical Engineering*, and the committee reported that more than 10,000 copies had been sold between April 15 and September 15, 1942.

"The address is not a handbook," the report stated; "It has no prescription for raising salaries; it deals with ideals; it starts off with a text like a sermon; it is a sermon, in applying a great truth to present-day life; it makes 'motivation of the individual' the true basis of professional life."

The report states that the committee's job now is "to drive in the wedge so well started; to promote understanding of the Wickenden address as indicating ways 'in which the professional stature of the engineer may be developed, and engineering in its broadest sense may be advanced.' "

ECPD Dinner Meeting

The dinner meeting held in connection with the ECPD annual meeting was attended by 33 officers, committee members, and representatives of the constituent societies. Colonel C. E. Davies, secretary, American Society of Mechanical Engineers, now serving with the Ordnance Department, Washington, D. C., acted as toastmaster. Speakers of the evening included chairmen and members of ECPD's committees, who spoke on the work of the respective committees; L. Austin Wright, general secretary, The Engineering Institute of Canada, and deputy director of Canadian Selective Service; Webster N. Jones, chairman of a committee to determine the manner in which the office of technical development should be set up within the War Production Board; and others.

In his preliminary remarks, Colonel Davies declared that we have not even started to establish engineering as a profession, so far as the public is concerned. He emphasized the importance of co-operative action by the various engineering groups in working toward this end, and pointed to the possibilities of co-operative action in this direction through ECPD. He declared that ECPD must increase the rate of growth of quality of engineering in order that the quality of engineering as a profession may be raised.

Speaking for the committee on professional recognition, C. F. Scott, chairman, characterized certification as the first step for young men entering the profession toward recognition as engineers. This, of course, requires adequate preparation. In striving for higher professional standards, engineers should work toward greater efficiency in handling technical problems, and then apply their talents to economic and social aspects of engineering problems. Doctor Scott outlined the history of the ECPD recognition program and discussed the activities of the committee for the past year. In conclusion, Doctor Scott said that engineering is like the church: It has many branches just as the church has many denominations, but the *spirit* of engineering is common to all branches. In this connection he compared ECPD to the Federal Council of Churches and pointed to similar opportunities for co-operative work.

Chairman D. C. Jackson of the committee on principles of engineering ethics, briefly reviewed the work of his committee. He first referred to the problems encountered in early efforts of the AIEE to formulate and adopt a code of ethics and related some of the early difficulties in bringing together the widely divergent views on the subject. Doctor Jackson stated that the same types of difficulties were encountered while working up the code that the committee prepared during the past year. He declared that such a code is of first importance in establishing the engineering profession in the minds of the public, stating emphatically that we must have "canons of ethics" before we can expect the public to recognize engineering as a profession.

Speaking for the committee on professional training, S. B. Kirkpatrick, member of that committee, first pointed out that the young engineer's growth levels off during the first few years after graduation from college and that professional training is needed most during these years. He reviewed briefly the accomplishments of the committee under previous chairmen, General R. I. Rees and O. W. Eshbach (F '37). He emphasized the necessity for the work of the committee to be carried on down below the "brass hat" level in order to be most effective. He expressed the opinion that the "Manual for Junior Engineers" now under preparation by the committee would prove to be of great assistance in this effort.

Three stages in the work of the committee on engineering schools were discussed by D. B. Prentice, chairman. The first stage comprised preparation of criteria to form the basis of the curricula accrediting program that has been the committee's principal accomplishment; this program then was carried out on a trial basis in the New England territory. The second stage comprised the extension of accrediting activities from the New England area to the remainder of the United States. In discussing this stage of the committee's work, Mr. Prentice outlined the procedure followed and emphasized the importance of accrediting by individual curricula. Having established standards for accrediting, Chairman Prentice discussed briefly the third state of the committee's work—the maintenance of those standards. He emphasized the importance of this stage, upon which the committee is just entering, and also the difficulties that may be encountered under wartime conditions.

Progress made by the committee on student selection and guidance during the ten years of ECPD's existence was reviewed by retiring chairman R. L. Sackett. He contrasted the early indifference of the engineering profession to selection and guidance work to the strong support that the program now enjoys. Originally, there seemed to be a feeling of satisfaction in allowing graduates of secondary schools to enter engineering colleges, leaving to the colleges the necessary "weeding out" process. There is now recognition of the fact that this is undesirable from two points of view: It is discouraging to the students who prove to be misfits in engineering colleges and must undergo the stigma of failure in that endeavor; and it imposes an unnecessary and undesirable burden on the engineering colleges by causing them to spend time training students who later prove to be misfits. Dean Sackett then reviewed briefly the experience of the committee with aptitude tests and pointed to the favorable results obtained during recent years.

TRIBUTE TO DEAN SACKETT

At the conclusion of Dean Sackett's remarks, Editor George A. Stetson of the American Society of Mechanical Engineers read a prepared tribute to Dean Sackett in recognition of his ten years' chairmanship of the ECPD committee on student selection and guidance. A portion of Mr. Stetson's statement follows:

"Long years of service in the field of engineering education have matured his wisdom and judgment. Experience with several generations of youth, with practicing engineers, and with the men and women of this nation has been his in rich measure. Some inner quality of alertness to men and events has kept his mind youthful and his vision clear. Although justified by years and the value of his accomplishments in enjoying the leisure that the vigor of his health would make a pleasure rather than a burden, he has preferred to pioneer in the field of selection and guidance of young engineers, and has devoted his time and energy to a new and important aspect of engineering education upon which the quality of future generations of engineers will indubitably depend. To the professional social scientist and the pedagogical psychologist he is a layman; but those who have known him and worked with him recognize that new ideas in any field coalesce into the substance of practicability when exposed to the influence of his maturity and experience.

"Dean Sackett, it is my privilege to extend to you on behalf of this group of your friends and fellow workers their congratulations and their tribute to you for your services in the field of engineering education and particularly for your ten years of leadership in developing the techniques of selection and guidance of engineering students. As your studies bear fruit in application in education and engineering, more and better leaders will be developed and a larger and more intelligent group of young men will rally to wise leadership. You cannot pass on to them your maturity and experience, but, when peace returns, following your example and aided by the influences you have set in motion, they will be able to distinguish between true leadership and false, bring education to serve the liberties and wellbeing of mankind, and build a nobler profession that will be the master and not the victim of applied science through a wise use of maturity and experience."

SELECTIVE SERVICE IN CANADA

A bird's-eye view of how the Canadian Selective Service system handles technical personnel was presented by L. Austin Wright, deputy director of the Canadian Selective Service. He described the Wartime Bureau of Technical Personnel established by the Canadian Institute of Chemists, the Canadian Institute of Mining and Metallurgy, and the Engineering Institute of Canada. The Bureau is operated as a unit of Selective Service system and is supported by the Canadian Government.

Mr. Wright reported that about 25,000 engineers and scientists had been registered, which represents from 80 to 90 per cent of the total of such men in Canada. Pointing out that Canada has established complete allocation of all man power, he said that no engineer can seek employment without first informing the Bureau of Technical Personnel. Likewise, no employer can hire an engineer without the permission of the Bureau. Employers are required to hold jobs open for employees at the close of the war, whenever the Bureau places an employee in a different job.

Canadian students in engineering and technical courses are allowed to complete their courses in a normal manner (Canada has not yet established accelerated courses), but students granted that privilege must make themselves available for employment by the armed services after graduation. One failure in any scholastic subject cancels the privilege to complete his course and makes him immediately available for service. Regarding postgraduate work, Mr. Wright said that no engineering postgraduate work is being allowed during the remainder of the war, except in special cases.

Under the Canadian complete-allocation plan, high-school students will be conscripted as necessary for the technical colleges in order to assure an adequate supply of technically trained men. Under this plan, high-school students would complete their courses after which they would be placed in technical colleges for regular collegiate training. Upon graduation, such men would be allocated between industry and the armed services to meet current requirements. Mr. Wright mentioned also that all women in Canada have been registered and many are being placed in industrial work and other positions.

WPB Announces Controlled-Materials Plan for War and Civilian Production

A long-range plan for controlling the flow of critical materials into war production—the Controlled Materials Plan—was announced November 2, 1942, by Donald M. Nelson, chairman of the War Production Board.

Evolved from existing distribution systems and from experience gained through their operation, the CMP has the approval of all governmental agencies participating in it. It was drafted after lengthy conferences with the Army, Navy, Maritime Commission and the WPB office of civilian supply as well as representative consumers and producers of materials.

TO SUPPLY MATERIALS WHEN NEEDED

The main purpose of the plan is to make certain that production schedules are adjusted within material supply so that production requirements are met. This will be accomplished by:

1. Adjusting requirements for critical materials to the supply.
2. Making the quantity and type of materials needed available at the time required to meet approved programs.

Allotments of critical materials will be made through seven claimant agencies, such as Army, Navy, office of civilian supply, and so forth, to prime contractors producing essential goods. Prime contractors, in turn, will divide the allotments with their subcontractors and suppliers.

Carbon and alloy steel, copper, and aluminum—the three most basic and critical materials—are the first controlled materials to be directly allotted under the plan, which becomes effective, in its transitory stage, in the second quarter of 1943 and will be in full operation by July 1.

VERTICAL ALLOTMENT

This method of distributing materials is, in effect, "vertical allotment." So far as the CMP is concerned, it will gradually replace the present priority system, including the Production Requirements Plan, which is on a horizontal basis.

Under PRP each firm, large or small, prime contractor or subcontractor, submits his own requirements to the WPB for approval, and receives an individual authorization to obtain materials.

Under the new CMP, prime contractors will prepare and submit a breakdown of all materials required for the approved end products on which they are working. The breakdown will comprise a bill of materials specifying not only what materials are required, but when they must be received to carry out the authorized program.

CLAIMANT AGENCIES

The claimant agencies are: Army, Navy, Maritime Commission, the aircraft scheduling unit, Lend-Lease, Board of Economic Warfare, and office of civilian supply.

Each claimant agency will break down its submission of requirements into materials for: 1. production; 2. construction and facilities; 3. maintenance, repair, and operating supplies. Requirements for construction and facilities, including industrial machinery and equipment, will be channeled through the construction and facilities branch of the office of program determination.

When requirements have been brought into balance with supply and the program of the various claimant agencies are approved, the WPB vice-chairman on program determination—who also is chairman of the requirements committee—will allocate with the advice of the requirements committee, authorized quantities of the three controlled materials to each.

The claimant agencies, in turn, will distribute these broad allotments among prime contractors by means of allotment numbers, which will constitute a right to receive delivery. The prime contractors will pass on the allotment numbers as necessary to their subcontractors and suppliers.

HOW THE PLAN WORKS

Materials other than controlled materials will continue to be distributed through the priorities system. Each company receiving an allotment number carrying an allocation of controlled materials also will receive a preference rating for use in obtaining other materials. A preference rating accompanied by an allotment number will be higher than other ratings of the same category, but will not take precedence over higher ratings. For example, *AA*-3, plus an allotment number, is higher than *AA*-3, without the number, but not as high as *AA-2X*. The preference ratings also will resolve conflicts which might otherwise occur in the production and delivery of manufactured items.

In order that sufficient amounts of materials in the form desired may be available, responsibility for directing the production of controlled materials rests in the controlled materials branches of the WPB. For instance, the iron and steel branch is responsible for steel, and the copper branch for copper. Production Directives, specifying the quantities and forms and shapes of material to be produced during a stated period of time, will be sent to most producers of controlled materials monthly. If orders beyond a specified capacity to produce are received, a producer must refuse them and notify the appropriate controlled-materials branch. If a consumer with an allotment number cannot place his order satisfactorily, he should appeal to and will be assisted by the branch.

As CMP goes into effect, the job of cutting out all nonessential production, military and otherwise, will be completed. Under CMP, each claimant agency will program the quantities of end products—guns, planes, Liberty ships, railroad cars, bedsprings, and so forth—most urgently needed for each quarterly period. From the bills of materials for each of these items the agency will make up a consolidated estimate of its total requirements. These detailed estimates for the second quarter of 1943 must be submitted by January 1, 1943. At the same time, similar estimates must be submitted for the remaining quarter of 1943 and the first quarter of 1944, together with general estimates for the first half of 1944, so that the requirements committee will have at all times a general picture of requirements, eighteen months in advance.

WAR ORDERS GIVEN PREFERENCE

When the allotments are made by the requirements committee, they will be transmitted to prime contractors through the claimant agencies. Manufacturers working on items such as tanks, ships, or aircraft, which generally are contracted for by or through a claimant agency, and are called "Class *A*" products—will receive their allotment with an allotment number directly from the agency. Producers of a list of "Class *B*" products, such as generators, hardware, kitchenware, electrical appliances, parts frequently incorporated in other products, and civilian items generally, will receive their allotments from their WPB industry branches, which in turn will receive allotments through the Office of Civilian Supply.

OVERALLOTMENT

Each claimant agency may allot for each month up to 105 per cent of its monthly allotment. This over allotment is intended to stimulate increased production from producers of controlled materials. Claimant agencies also are authorized to make allotments for future quarters on the basis of declining percentages of allotments established for the current quarter. These percentages are:

For the quarter immediately following the one for which a definite allotment has been made, 80 per cent; for the next following quarter, 60 per cent; for all later quarters, 40 per cent.

The plan will be flexible enough to permit limited amounts of material to be given out without allotment numbers. Special provision, for instance, is made for allotments of controlled materials to warehouses so that they may handle small orders without allotment numbers.

A new form of inventory control is to be established with the requirement that every primary or secondary producer whose inventory of all controlled materials is in excess of a specified amount must submit an inventory statement showing his position at the end of each calendar quarter not more than 15 days later.

TRANSITION PERIOD PROVIDED

A timetable for the transition from existing systems to full operation of the CMP is provided. The first bills of materials will be assembled by the claimant agencies during November and December 1942 and on January 1, 1943, the agencies will submit their first estimate of requirements to the branches handling controlled materials, with copies to the requirements committee.

By January 15, 1943, the controlled materials branches will have analyzed the requirements and made preliminary reconciliation to the extent possible between requirements and supply. At the same time, the claimant agencies and prime consumers will be developing information necessary in making final allotments, to be in readiness for distribution of allotments to them by the requirements committee.

On February 1, 1943, the requirements committee will make allotments of controlled materials to claimant agencies for the second quarter of 1943. During February and early March, 1943, distribution of allotments will be made by claimant agencies to prime consumers, who in turn will divide their allotments with their secondary consumers and suppliers.

By March 15, 1943, users of controlled materials will have placed authorized orders for April delivery and for later months, as authorized. Subsequently the controlled materials branches will watch placement of orders on mills and mills' shipments, and give assistance in placing orders to authorized users of controlled materials who are unable to obtain mill acceptance of authorized orders.

On July 1, 1943 CMP will be in full operation. Until that time existing procedures, including preference rating orders and PRP certificates and individual material allocations under M orders will continue in effect for consumers who have not been able to qualify under CMP.

Those remote secondary consumers who have not obtained their allotments under CMP in time to meet requirements for the second quarter of 1943 will be authorized to continue purchases under PRP equal to their first quarter authorizations.

To prevent duplication each company operating under PRP will be required to cancel authorizations made under PRP in equal amount for CMP allotments and the total authorizations outstanding at any time will not be permitted to exceed available supply. Orders bearing CMP allotment numbers will be given preference at mills over PRP orders and other rated orders not under CMP.

Official copies of a pamphlet outlining the new Controlled Materials Plan are being made available for free and general distribution from WPB field offices.

War Department Merges Specialist Corps and Army Officer Procurement System

As already announced in the news press, the Army Specialist Corps will be discontinued December 31, 1942, by direction of Secretary of War Henry L. Stimson. As previously reported in *Electrical Engineering* (*EE, Aug. '42, pp. 440–2; Sept. '42, p. 490*) the Specialist Corps was brought into being during the spring months of 1942 for the purpose of obtaining from civil life especially qualified technical and professional men for duties with the Army where military training and experience are not essential. The procurement functions and basic policies of the Corps are being absorbed by a new War Department agency which has been designated as the Officer Procurement Service; this new agency also is taking over from the office of the Adjutant General essentially all officer-procurement functions. Thus, the OPS will provide a single consolidated channel for officer-procurement for all branches of the Army.

In general, the requirements for the commissioning of Army officers, including technical and other specialists, have been tightened materially. To be eligible for consideration now, a candidate must be at least 35 years old *and* in Selective Service class III-A, III-B, or certain restricted categories of IV-F, and must have "civilian training and experience for the particular position beyond that normally provided at Officer-Candidate Schools." Exception to these rules is allowable "only where there is critical need for the services of a particular individual or where the individual is within a 'scarce category' of specialized skill in which not enough trained men to fill the requirements of the armed forces are available at the time required." Airplane pilots, radio mechanics, and electronics specialists are typical of the "scarce categories" referred to.

Contrary to widespread misconception, the Army Specialist Corps was not a separate functioning unit of the Army like the Engineer Corps or the Medical Corps.

The ASC of itself was only a procurement service. It could not select and appoint qualified specialists and then assign them to appropriate jobs in the Army structure. The ASC could act to procure the services of a qualified candidate only upon the specific request of one of the branches or services of the Army. Further, the candidate, upon appointment as an ASC officer became a member *not* of the ASC, but of the requesting agency. The only functioning members of ASC, as such, were the headquarters-staff personnel in Washington, D. C., and in the 35 field offices that had been established in collaboration with the Officer Procurement Branch of the Adjutant General's office. These field offices are in process of translation into field offices of OPS.

Subject to the recommendations of the commanding officers of the Army branches to which individual ASC appointees have been assigned, subject to a review of the candidate's physical-examination report by a member of the Surgeon General's staff, and subject to the final and determining action of the Secretary of War's Personnel Board, each such ASC appointee will be tendered a commission in the Army of the United States, if the appointee filed a request for such commission no later than December 1. All appointees from whom requests for AUS commission were not received by OPS by December 1, 1942, will be given an honorable discharge as of December 31, 1942. In similar fashion, members of the ASC headquarters staff will be tendered commissions or discharges according to circumstances. The headquarters staff members are subject to assignment to other branches of the service as the need for their services in headquarters diminishes with the perfection of the consolidation.

By order of the Secretary, ASC appointees in service or ordered to active duty by October 31 are subject to the procedure just outlined, and are eligible for conversion into AUS officers "at the same rank or one rank lower, but in no case at a higher rank." Once in the AUS, of course, the individual is eligible for promotion under established procedures, an opportunity for which he was not eligible as an ASC officer.

A $5,000,000 Program to Curb Industrial Accidents

Faced by mounting casualties on the home front already exceeding those on the fighting front, a program to curb the sabotage of manpower by accident is being organized by the War Production Fund to Conserve Manpower of National Safety Council. As reported previously (*EE, May '42, p. 333*), William A. Irvin, former president of the United States Steel Corporation, was named chairman of the Fund; Thomas W. Lamont of J. P. Morgan and Company is treasurer. A goal of $5,000,000 has been agreed on, this sum to be raised from business and industry throughout the United States.

A national committee of more than 600 members has been formed, with an executive committee of 74—both made up of senior executives in nationally prominent firms. A preliminary canvass through the national connections of major companies has yielded more than a million dollars in cash and nearly another in oral commitments. At present regional campaigns are getting under way in major industrial centers from coast to coast.

Many big firms have contributed heavily to the War Production Fund despite the fact that they already have excellent safety programs in effect. They are aware that some outside agency with specialized experience is needed to co-ordinate safety off the job, where three out of five accidents affecting workers happen, and to furnish technical assistance in handling the brand new problem of safety for the army of women in industry and of safety in the homes temporarily neglected for drill press and assembly bench. The same goes for accident-prevention methods to protect teen-age boys and older men now filling the shoes of men drawn into the armed forces.

The National Safety Council has formulated a plan for putting the $5,000,000 to work. No radical departures from previous practice are contemplated. The general strategy will be to speed up the existing program, and to get more coverage, more technical assistance in the field, and above all, more public co-operation in accident prevention.

WPB Industry Branches Reorganized

Reorganization of industry branches of the War Production Board, giving them greater strength, is being undertaken by the office of program determination under the direction of Ferdinand Eberstadt, vice-chairman of WPB, and Ernest Kanzler, director general for operations. One of the major reasons for strengthening branches is to enable them to handle the additional burdens to be placed on them by the new Controlled Materials Plan (*this issue, pp. 630–1*).

To as great an extent as possible, each industry branch will follow a similar pattern. Many of the functions of the office of the director general for operations will be assigned to branches, making them responsible for all operating phases such as the execution of programs, policies, and procedures established for the resources assigned to branches.

Permanent connection with industry will be maintained through active industry advisory committees assigned to each branch. Claimant agencies—Army, Navy, Maritime Commission, Office of Civilian Supply, Lend-Lease, and so forth—will assign permanent representatives to each branch. Permanent connection with labor also will be maintained through representatives assigned to each branch.

Industry advisory committees assigned to the branch will endeavor to put upon industry a greater measure of responsibility for meeting war production problems. They will get advice from the industry on methods of increasing supply where shortages exist, of controlling the distribution resources and of eliminating nonessential uses of resources. Likewise, they will obtain data on available and anticipated supply of resources and requirements for civilian use and for maintenance, repair, and spare parts.

Representatives of governmental claimants in the industry branch will submit data on requirements for resources and advise and assist in apportioning the available supply in accordance with the determinations of the requirements, program adjustment, and facilities committees. Claimants, in addition will assist in the processing of various forms controlling the distribution of resources.

Canada Represented on Production and Resources Board

Canada has recently been admitted as a member of the Combined Productions and Resources Board, a council maintained jointly along with the United States and Great Britain in recognition of the interlocking war economies of the three nations. She is represented by the Honorable C. D. Howe, Canadian minister of munitions and supply, who takes his place on the board with Donald M. Nelson and Oliver Lyttelton.

A joint war production committee of the United States and Canada has been operating for almost a year, and has achieved pronounced success in increasing war output by arranging rapid exchanges of supplies to avoid production delays, reducing duplications, breaking transportation bottlenecks, eliminating tariff and other barriers, and revising specifications to increase the number of common-type weapons. Typical of the co-operative industrial relationship between the two countries is the record of 62 Liberty-type merchant ships built in Canada with steel plate made available by the United States.

According to estimates, one-quarter of Canadian production is now going to fill United States orders, while about one-third of Canada's war goods is being produced for Great Britain.

Wider Distribution of War Orders to Small Manufacturers Planned

Procurement officials from the United States Army, the United States Navy, and the War Production Board recently issued directives indicating a renewed effort to bring smaller war plants more effectively into the nation's war-production program. Salient points in the memorandums issued by Lieutenant General Brehon B. Somervell to the chiefs of the Services of Supply and by Under-Secretary of the Navy James V. Forrestal were reiterated as follows in the general statement made by Lou E. Holland, deputy chairman of the WPB on Smaller War Plants and chairman of board of the Smaller War Plants Corporation:

1. The Smaller War Plants Division, basing its information on advance requirements of suitable items sent out by the War Department or the procurement agency, will inform these offices of sources of supply among the smaller plants with suitable production facilities to furnish necessary items by required delivery dates.

2. Maximum possible subcontracting will be used in the production of items which, because of their complexity, must be awarded to the large organizations.

3. The Smaller War Plants Division agrees to maintain a list of small plants adaptable for the production of specific items, and to co-operate with the procurement agencies in the placement of prime or subcontracts by proving the suitability and competence of the proposed small plant and certifying as to its credit.

4. The department or agency, in conjunction with the Smaller War Plants Division, will examine existing prime and subcontracts with a view to securing further subcontracting whenever possible, and the agencies will attempt to secure the agreement of the contractors to further subcontracting whenever practicable. Cancellation of orders for new machine tools and equipment will be made in all cases where the corresponding operations can be handled by subcontracting, even though it is necessary, as the result of such subcontracting, to amend the contract to provide for increased costs because of less efficient methods of production.

5. The Smaller War Plants Division will provide planning, engineering, and production assistance to small plants, so that they may execute in a proper and satisfactory manner any contracts undertaken.

Foreign Patent Specifications to Be Available

Drawings and specifications of foreign-owned patent applications seized by the Office of the Alien Property Custodian will be printed and made available to American industry at a nominal price, Leo T. Crowley, Alien Property Custodian, announced recently. These applications ordinarily cover latest developments in patentable fields and many can improve American processes and devices. This departure from the traditional secrecy with which patent applications heretofore have been cloaked, it was stated, is in accordance with instructions from President Roosevelt that the Office of Alien Property Custodian use all means at its disposal to put enemy-owned property and patents to work in support of the nation's war effort.

Publication of the printed copies of patent applications will begin during December. Applications will be listed as they are printed, in classified order, in the *Official Gazette* of the United States Patent office. Announcement of the cost of copies and of the method of purchase will be made in the same publication. Meanwhile, the alien property custodian will make the information contained in the files of these applications available, in so far as is practical, to any person residing in the United States having a legitimate interest therein.

Any registered patent attorney may obtain permission to inspect (and make copies of) the file of a vested application (other than an application which stands under secrecy order) upon filing with the patent prosecution section, Office of the Alien Property Custodian, Washington, D. C., a request upon a form, "petition to inspect," which will be furnished upon request by the Custodian's Washington, D. C., Chicago, Ill., or New York, N. Y., offices.

Air-Raid Warning Device

Air-raid warning device developed in the carrier current laboratory of the General Electric Company, Schenectady, N. Y., provides audible and visible warning signals. It can be plugged into a house circuit and is designed for use on 720-cycle carrier-current systems. Operated by a special control board attached to the carrier-current transmitter at the power station, it would relieve telephone lines for other purposes during a raid by using existing power lines

War Production Drive Speeding Output. Labor-management committees, with the co-operation and assistance of the War Production Drive Headquarters, Washington, D. C., are conducting campaigns to stimulate production in American factories, mines, collieries, railroads, mills, and war plants. Names of the 1,600 plants in the United States now having labor-management committees and the committee chairmen have been published in a pamphlet available from the War Production Drive Headquarters, Raleigh Hotel, Washington, D. C. Basic to the formation of these committees is the exchange of ideas between labor and management and the granting of awards for increased output (*EE, September '42, p. 492*). The WPD headquarters has received a number of unsolicited reports from industries which indicate that since those plants inaugurated the campaign their production has increased 35 per cent in some cases.

Technical Books Requested for War Prisoners

Requests for technical books for allied prisoners of war in enemy prison camps have recently been forwarded to AIEE headquarters by the War Prisoners' Aid of the Young Men's Christian Association. The "Men of Science—Prisoners of War" Service of the YMCA War Prisoners' Aid is attempting to supply the needs of professional men and advanced students who are facing perhaps many more months of monotony in prison camps. It is stated that arrangements with the censors make it possible to send books that were published before November 1941. Books on the following subjects are currently requested:

1. Electrical supply and installation.
2. Modern power station equipment.
3. Electric traction.
4. Electrical engineering classified examples.
5. 20 to 30 Greek books on science.

In order to avoid possible censorship difficulties, please send the titles of any books available to R. D. Jameson, technical adviser, Library of Congress Annex, Washington, D. C. He will send instructions about getting the books to the War Prisoners' Aid.

The War Prisoners' Aid has already furnished many scientific books and reports the gratitude of the prisoners who receive them as they are thereby enabled to keep up with their scientific or professional work.

Power Pool Operates in Northwest. A significant development in the history of the electric utility industry in the Pacific Northwest occurred recently—connection of all major electric systems in one power pool. The move substantially increases the area's capacity to handle new war loads. The new supersystem, in which participants linked both their hydro and steam plants, increases over-all capacity by capitalizing on the fact that peak loads come at varying times and seasons in the different communities. Interchange of power is possible even though there is no direct connection between certain cities because energy is delivered through the supercircuit to all cities connected to the loop.

INDUSTRY • • • • •

Position to Be Filled Through Civil Service Examinations

Notice of the following position, which will be filled through civil service examinations, is published here as a service to members of the Institute. Application forms and full information as to requirements for examinations may be obtained from the secretary of the Board of the United States Civil Service Examiners at any first- or second-class post office, or the United States Civil Service Commission, Washington, D. C.

Materials Inspector. $2,600 a year salary. No age limits. To qualify for the position, applicants should show appropriate experience in the inspection, or testing for compliance with specifications, of a wide variety of either electrical or mechanical equipment. Broad progressive mechanical or skilled production experience in the manufacture of mechanical or electrical equipment, or experience as journeyman machinist, or journeyman toolmaker is acceptable. Applicants should be able to make written reports clearly and intelligently. The United States Civil Service Commission will continue to accept applications until further notice. No written test is required. Applications are not desired from persons engaged in war work unless a change of position would result in the utilization of higher skills possessed by the worker.

Westinghouse New District Office. The Westinghouse Lamp Division, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., recently replaced its branch office organization in Philadelphia with a complete district office. This new Middle Atlantic District is managed by Harry A. Croasdale, formerly assistant manager of the Middle Western District and at one time branch manager in Philadelphia. The new district will include seven counties in New Jersey, the entire states of Delaware, Maryland, and Virginia, the District of Columbia, and parts of West Virginia and Pennsylvania.

IT and T Associate Companies Merge. The International Telephone and Radio Manufacturing Corporation, and the Federal Telegraph Company, associate manufacturing companies in the United States of International Telephone and Telegraph Corporation, New York, N. Y., have been merged to form the Federal Telephone and Radio Corporation, Newark, N. J. This organization is now devoted almost entirely to the production of communication and radio equipment for the war effort. Construction of the first unit of a new factory which will house the new corporation will start shortly.

OTHER SOCIETIES •

Electrochemical Society Forms New Unit. The corrosion division, a new unit of The Electrochemical Society, was formed October 9, 1942, at the society's fall convention in Detroit, Ill. Growing out of the interest of a large number of the members in corrosion, the division is aimed at co-ordinating these interests with organizations and individuals already active and interested in this field, and will provide a channel for papers and symposia. Officers of the division for 1942–43 elected at the meeting are: L. G. Bogart, research and development laboratories, Crane Company, Chicago, Ill., *chairman;* H. H. Uhlig, research laboratory, General Electric Company, Schenectady, N. Y., *vice-chairman;* and R. H. Brown, metallurgical division, aluminum research laboratories, Aluminum Company of America, New Kensington, Pa., *secretary.* Nonmembers who are especially interested in corrosion work are invited to join The Electrochemical Society and to participate in the work of the corrosion division.

ASA Issues Revised Standard

A newly revised standard "Illuminating Engineering Nomenclature and Photometric Standards" (Z7.1–1942) developed under the leadership of the Illuminating Engineering Society, has been recently published by the American Standards Association. This standard provides the practical language of illuminating engineering and photometry, and in addition, contains tables on relative luminosity factors; wave-length units; relative erythemal factors; distribution coefficients for equal-energy stimulus; relative magnitudes of units of illumination and relative magnitudes of units of brightness; a listing of units, symbols, and abbreviations; and equivalents and conversion factors. Most of the fundamental definitions included in this standard have been adopted by the International Commission on Illumination, and are included in the multilingual vocabularies compiled by that commission and by the International Electrotechnical Commission.

Copies of this standard are available at 25 cents per copy from the ASA, 29 West 39th Street, New York, N. Y.

Future Meetings of Other Societies

American Association for the Advancement of Science. Annual meeting, December 30, 1942, New York, N. Y.

American Institute of Mining and Metallurgical Engineers. Annual meeting, February 14–18, 1943, New York, N. Y.

American Physical Society. 252d meeting (annual) December 28–30, 1942, New York, N. Y.; 253d meeting, December 29, 1942, Los Angeles, Calif.

American Society of Civil Engineers. Annual meeting, January 20–22, 1943, New York, N. Y.

American Standards Association. Annual meeting, December 11, 1942, New York, N. Y.

Society for the Promotion of Engineering Education. Middle Atlantic states section winter meeting, December 5, 1942, New York, N. Y.

Technical Valuation Society. Annual forum, December 12, 1942, New York, N. Y.

AAAS Engineering Section to Meet

Section M (engineering) of the American Association for the Advancement of Science will hold its sessions in connection with the 1942 annual meeting of AAAS on Wednesday, December 30, at the Hotel Commodore, New York, N. Y.

The 1942 program of Section M will feature two important subjects in fields where engineering must lean heavily on science. At the morning session the subject will be aeromedicine.

The subject of the afternoon session will be the dehydration of foodstuffs.

The program of the meeting:

Wednesday, December 30

9:00 a.m. Morning Session

1. "The Tilting Ballistocardiograph: Apparatus for Recording the Thrust of the Heart," **Robert W. Wilkins,** M.D., department of clinical research and preventive medicine, Robert Dawson Evans Memorial, Boston, Mass.

2. "Development of Instruments for Test and Classification of Flight Personnel," **E. Lodwig,** chief engineer, and **J. Zaleski,** design engineer, Mobile Refrigeration, Inc., New York, N. Y.

3. "The Application of Engineering Principles to Clinical and Aviation Medicine," **Alvan L. Barach,** M.D., associate professor of clinical medicine, Columbia University, New York, N. Y.

12:00 m. Luncheon Session

"The Romance and Engineering of Food Preservation," **Willis R. Woolrich,** dean of engineering The University of Texas, Austin.

2:30 p.m. Afternoon Session

1. "Theory of Processes," **H. J. Masson,** professor of chemical engineering, College of Engineering, New York University, New York, N. Y.

2. "Application of Theory to Manufacture," **Graham L. Montgomery,** associate editor, *Food Industries,* New York, N. Y.

3. "Military and Civilian Practice," Captain **Robert P. Melson,** Subsistence Research Laboratories, United States Quartermaster Corps, Chicago, Ill.

ASA 1942 Price List Published

The American Standards Association has announced the publication of its newest list of American Standards for 1942. More than 550 American Standards are listed, of which 71 represent new and revised standards approved since the last issue of the list (February 1942). These are marked with an asterisk. There is a separate section for American War Standards, standards developed specifically for the war effort. Another section is devoted to American Safety Standards, which are also of great importance to the war effort in connection with the President's program for the conservation of man-power. Other standards include definitions of technical terms, specifications for metals and other materials, methods of test for the finished product, dimensions, and so forth. They reach into every important engineering field and serve as a basis for many municipal, state, and Federal regulations.

In each case, the standards approved by the ASA represent general agreement on the part of maker, seller, and user groups as to the best current industrial practice. More than 600 organizations are taking part in this work. This list of American Standards for 1942 will be sent free of charge to anyone writing in for it. Requests should be addressed to the American Standards Association, 29 West 39th Street, New York, N. Y.

American Section of CIE Revived

The American section of the Chinese Institute of Engineers, first organized in the United States in 1919 under the name of Chinese Engineering Society, has been reorganized. At a meeting held recently the following officers were elected:

President: L. F. Chen; *vice-presidents:* A. T. Liu, P. H. Chin; *executive officers:* T. C. Hsiung, C. H. Tang, C. H. Wang.

Following its inception in the United States the society was reorganized in China under its present name and maintained an American section. After the outbreak of the war with Japan, many members in America were called to duty in China and consequently during the last few years the American section has been inactive. Realizing the importance of reviving its activities, the Chinese engineers in the United States are forming local CIE chapters in New York, N. Y., Washington, D. C., Detroit, Mich., Ithaca, N. Y., Boston, Mass., and on the west coast. The total membership of the American section is expected to be between 300 and 400 soon and is divided into the following classifications:

Mechanical engineering, electrical engineering, civil engineering, chemical engineering, mining and metallurgical engineering, and aeronautical engineering.

Besides arranging for local engineering lectures and discussions and also publishing a bimonthly CIE News Bulletin and a CIE Engineering Journal, contacts have recently been established with several leading U. S. engineering societies and institutes with a view to exchange of publications, to arrange for or participate in special lectures and, in general, to assist and give strength to this newly revived organization of Chinese engineers, the only one of its kind in the United States. In co-operation with this CIE policy, AIEE publications are already being exchanged with the CIE American section and the AIEE New York section plans to invite CIE members to its meetings.

LETTERS TO THE EDITOR

INSTITUTE members and subscribers are invited to contribute to these columns expressions of opinion dealing with published articles, technical papers, or other subjects of general professional interest. While endeavoring to publish as many letters as possible, Electrical Engineering reserves the right to publish them in whole or in part or to reject them entirely. Statements in letters are expressly understood to be made by the writers. Publication here in no wise constitutes endorsement or recognition by the AIEE. All letters submitted for publication should be typewritten, double-spaced, not carbon copies. Any illustrations should be submitted in duplicate, one copy an inked drawing without letter, the other lettered. Captions should be supplied for all illustrations.

U. S. Patent System

To the Editor:

Lately there has arisen a new crop of crusaders who consciously or unconsciously are attempting to wreck the American patent system, which in the past has contributed so largely to our material progress. The president of the United States has wisely appointed a patent-planning commission of eminent men, charged with the duty of making a survey of the entire patent situation and making recommendations for the improvement of our patent system. But the crusaders, not willing to wait for the report, have introduced in congress a flock of bills, most of which would emasculate completely the patent system and wipe out any benefits to be derived therefrom by the public.

Engineers have a vital interest in the patent system and yet the majority are woefully ignorant of the contents of the proposed bills. Many have never troubled to apply engineering thinking to the garbled statements and to reports regarding the functioning of the patent system which appeared in the newspapers. The articles given in the accompanying bibliography should be profitable reading to every engineer.

The Engineers' Society of Milwaukee and affiliated societies have felt that some positive action should be taken to arouse the interest of engineers, and a special meeting of all technical men was held on October 1, 1942, at which Mr. William F. Buckley, a Milwaukee patent attorney, spoke on the "Constitutional Right for Protection to Inventions", and Mr. W. E. Crawford (F '36), spoke on the "Future of Inventions in Relation to Science and Engineering in Itself."* As a result of the ensuing discussion, a resolution was passed asking congress to defer any patent legislation until the report of the patent-planning Commission was obtained.

It is the writer's belief that it would be profitable for other engineering groups to hold similar meetings. The national engineering societies could well afford to arrange for discussion of the subject at one of their national meetings. The engineer, as a professional man, owes it to the public to be informed in such matters and to help in guiding the attention of the public towards an unbiased appraisal of the role which patents and inventions play in the social and economic structure of the United States.

REFERENCES

1. The Relation Between Patent Practices and the Antimonopoly Laws—Part I, **G. S. Rich.** Patent Office Society *Journal*, volume 24, February 1942, pages 85–106; Part II, March 1942, pages 159–81; Part III, April 1942, pages 241–83; Part IV, May 1942, pages 328–56; Part V, June 1942, pages 422–37.

2. Inventions, Patents, and Victory, **J. D. Cunningham.** Patent Office Society *Journal*, volume 24, July 1942, pages 451–7.

3. Patent Laws in Our Social Scheme, **Nelson Littell.** Patent Office Society *Journal*, volume 24, August 1942, pages 520–30.

4. We Depend on Invention, **Lawrence Langner.** Patent Office Society *Journal*, volume 24, August 1942, pages 545–64.

5. Chemists Should Read Patents, **R. Frankl,** Patent Office Society *Journal*, volume 24, August 1942, pages 565–74.

6. An Opportunity for the National Patents Planning Commission, **Norman Holland.** Patent Office Society *Journal*, volume 24, September 1942, pages 592–8.

7. The American Patent System, **Wallace H. White, Jr.** Patent Office Society *Journal*, volume 24, September 1942, pages 599–628.

ARTHUR SIMON (F '13)

(Patent attorney and engineering consultant, Milwaukee, Wis.)

The Effect of Initial Conditions on Subharmonic Currents

To the Editor:

The paper on "The Effect of Initial Conditions on Subharmonic Currents in a Nonlinear Series Circuit," by Stephen J. Angello in the AIEE *Transactions*, volume 61, 1942, September section, pages 625–7 was particularly interesting. On the basis of a series of experiments, we should like to add to this very instructive paper the following information on the stability of such oscillations.

In a test circuit like the one used by Mr. Angello, which is composed of a capacitor, an iron-cored inductor and a small resistor, it is found that for certain combinations of capacitance and rms values of impressed voltage, sustained subharmonic oscillations of several orders can be obtained. The order of subharmonic oscillations is defined by the number of complete cycles of impressed voltage during which the current wave completes one cycle. For a fixed inductor and a constant capacitance, there will be a range of voltages for which sustained subharmonics of a particular order can be initiated. This range is usually rather sharply defined. The shaded area of Figure 1 shows a region, within which subharmonic oscillations could be initiated and sustained. It will be noted that the region defining subharmonic oscillations of the fourth order is surrounded by a region of oscillations of the second order.

---

* The essential substance of this address is scheduled for early publication in *Electrical Engineering*.

The most common subharmonic oscillation is that of order three, and since the region for this order is rather large especially for low values of resistance in the circuit normally little trouble will be encountered in sustaining oscillations of this order.

There is usually some difficulty involved in the initiation of subharmonic oscillations of the second order, or of oscillations of higher order than three. The voltage range which permits the initiation of sustained oscillations of one order is rather narrow for the capacitor used in our experiment—in the neighborhood of four or five volts, except for a range of capacitances from about 550 to about 650 microfarads. This can be seen readily from the graph in Figure 1, where capacitance is plotted versus rms values of impressed voltage.

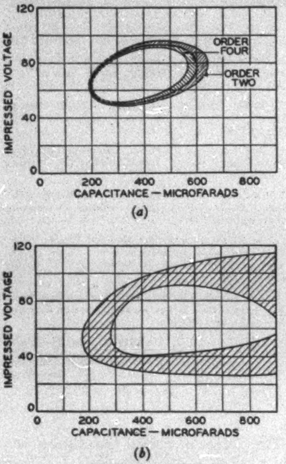


Figure 1. Regions of subharmonic response; resistance, 1.27 ohms; inductor, 110-volt winding of a $7^{1}/_{2}$ kva, 110–220-volt transformer

(*a*) *Orders two and four* (*b*) *Order three*

Most or all of the following conditions must be fulfilled, if the oscillations are to be sustained:

(*a*) The capacitor must be initially charged to a minimum value.

(*b*) The initial flux linkages due to residual magnetism must have a definite value.

(*c*) The circuit must be closed at a certain point on the voltage wave.

(*d*) The resistance of the test circuit must not be too high.

It appears reasonable that oscillations of this kind are difficult to sustain even if the above conditions are fulfilled, since the peak values of currents of higher order subharmonics are often extremely high, and poor regulation of the voltage supply causes immediate changes of impressed voltage which in all likelihood will stop the oscillations.

Therefore it seems permissible to conclude that these oscillations of the second order and of orders higher than three might be assumed unstable, since they might persist for only a short duration and are apt to disappear because of the slightest disturbance of the circuit. The abnormally high currents associated with such subharmonic frequencies favor this "unstability" due to line regulation.

CHARLES F. SPITZER (Enrolled Student)

THOMAS P. EVANS (Enrolled Student)

(Dunham Laboratory, Yale University, New Haven, Conn.)

Temperature and Electric Stress in Impregnated-Paper Insulation

To the Editor:

In the interesting paper "Temperature and Electric Stress in Impregnated-Paper Insulation" by J. B. Whitehead and W. H. MacWilliams, published in AIEE *Transactions*, volume 61, January section, pages 10–13, which owing to war conditions reached me only recently, power factor-stress curves of the characteristic shape given by liquids containing ions the motion of which is limited by solid barriers are obtained.

The stress corresponding to maximum power factor in these curves may be used, in conjunction with other data, to calculate the concentration, mobility, and effective radius of the ions concerned—information which cannot so readily be obtained by any other method. It is therefore important that the factors controlling the shape of the curves should be correctly explained.

Possibly owing to a need for brevity, the authors have given an explanation which is correct only in regard to the falling portion of the curve. Regarding the rising portion, they write ". . . increasing stress in the low range is accompanied by increase in the lengths of the excursions of the free ions . . . and consequently increases in conductivity and loss."

It is true that an increase in the ionic path proportional to increase of stress will result in an increase of total loss, but the same is not true of the conductivity, as distinct from the current conducted. A liquid in which ionic path is proportional to stress will possess a constant conductivity and therefore a constant loss angle up to that stress at which further motion of the ions is impeded by some barrier. The cause of the observed initial increase in loss angle must be sought elsewhere.

In a quantitative analysis of curves of this type, I have given an explanation of the rising portion of the curves which appears to check adequately with experimental data.[1] This is based upon the fact that the applied stress must, during the greater part of each cycle, segregate the positive from the negative ions, and thus reduce their effective rate of recombination. The rising part of the curve is thus due to a temporary increase in the number of ions so long as the field is applied, and dependent upon the magnitude of the field. The numerical value of the increase may easily be calculated, and agrees with experimental results at stresses which are not too low. At extremely low stresses, a further correction must be applied due to the image forces upon the ions, from the proximity of the walls of the pores in which they exist.

It is of historical interest that curves of this shape were reported so long ago as 1928 and it is remarkable that their value as a means of research into the characteristics of ionisation in liquid dielectrics should have escaped notice until the present time.[2]

REFERENCES

1. Dielectric Loss in Thin Films of Insulating Liquids, C. G. Garton. Institution of Electrical Engineers *Journal*, volume 88, April 1941, pages 103–20.

2. Dielectric Losses in Oil-Impregnated Paper, E. R. Le Ghait. *Electric Journal*, volume 25, April 1928, pages 187–93.

C. G. GARTON

(British Electrical and Allied Industries Research Association, London, England)

Detecting the Saboteur in Gasoline Fires

To the Editor:

The article "Detecting the Saboteur—Static Electricity—in Gasoline Fires" by Robin Beach (*EE, October '42, pp. 501–08*) recalled to the writer an actual happening that might be of interest to readers.

Two 1,500-barrel steel tanks with about 12-inch manholes in the center of the top were set near a railroad siding. These tanks were set on a limestone hill (typical in Kansas) with the upper soil removed to provide a flat place on which to set them. Crude oil was pumped into these tanks so that it could be loaded into tank cars for shipping. This crude oil was fairly high gravity with lots of dissolved gasses, and was pumped through a 4-inch pipe line from the oil field some ten miles away.

The writer, being a young engineer, was in charge of engines and pumping equipment and had nothing to do with pipe lines and tanks. The line was run in the usual oil-field manner covered up in the ground for about ten miles. The end of the line ran up over the tanks and was supported by placing wood, four inches by four inches, on top of the tank for support (Figure 1). The necessary ells, valves, and so forth allowed the line to discharge into the tanks through the manholes in the tops and were centered in the manholes as nearly as possible, all of which kept the well-grounded pipe line well insulated from the ungrounded tank. This made excellently designed spark plugs, since the pipe line was everywhere (accidentally of course) well insulated from the tanks and was well

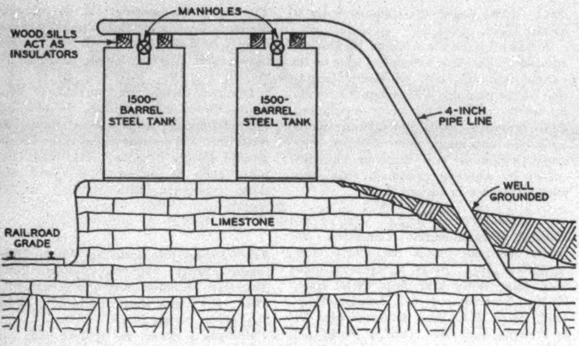


Figure 1

grounded itself. The manholes were always giving off highly inflammable gasses right in the manhole opening where the spark would occur, and with the tanks partly full of highly inflammable oil, conditions could not have been better for a static discharge between the discharge pipe and the edge of the manhole, whenever a highly charged cloud passed over just before a rain. After or during a rain the tanks would be more or less grounded by the limestone becoming wet.

The one in charge of construction and pipe-line officials for the company were told that a fire would be sure to burn the tanks up and that three feet of bonding wire and 30 minutes of work would probably save them. The tanks burned. It was too simple to bond them.

A. D. STODDARD (M '36)

(Vice-president, Haliburton Oil Well Cementing Company, Duncan, Okla.)

Electrical Heating of Overhead Conductors

To the Editor:

With regard to the electrical heating of overhead conductors described in the paper, "Sleet Problems on Electrified Railroads," by H. F. Brown (AIEE *Transactions, August section, pp. 589–93*), there is quoted from Pender's Handbook this formula:

$$I=K\sqrt{\frac{Td^3}{r}}$$

where

T is the temperature rise in degrees centigrade

d is the diameter of the conductor in inches

r is the resistance of the conductor in ohms per circular mil-foot at final temperature

K is a constant depending upon surface condition of wire; whose value is (approximately) 800 in still air and 1,000 in open air for solid wire; and 1,200 in open air for stranded conductors.

This shows that the heating current is directly proportional to the square root of the temperature rise, to the square root of the cube of the conductor diameter, and inversely to the square root of the resistivity of the conducting material at the final temperature.

The writer has constructed a nomogram for the graphical solution of this equation merely by drawing three lines across the scales (Figure 1). These computing secants are numbered one, two, three, and are drawn in that order, each one cutting the three scales marked thereon at the appropriate values. The use of the chart is therefore self-evident.

The computation drawn on the chart shows, for instance, that a solid copper conductor 1.31 inches diameter (d) at a final temperature of 100 degrees centigrade, being a rise of 80 degrees (T), will be carrying 3,620 amperes (I) in open air. The resistance of the mil-foot of copper wire at 100 degrees, as taken from the separate graph at upper right, is 13.7 ohms (r). Thus "one" is drawn through $r=13.7$ and $T=80$, and produced to pivot scale P. "Two" is drawn thence to 1.31 on d, through pivot scale Q; and "three," thence backwards to 1,000 on K, through I, which it cuts at 3,620 amperes, the required value.

For compactness the chart is conceived as folded over on the d scale so that the r scale and the K scale, which has the only three above mentioned points thereon, coincide.

CARL P. NACHOD (A '07)

Vice-president, Nachod and United States Signal Company, Inc., Louisville, Ky.)

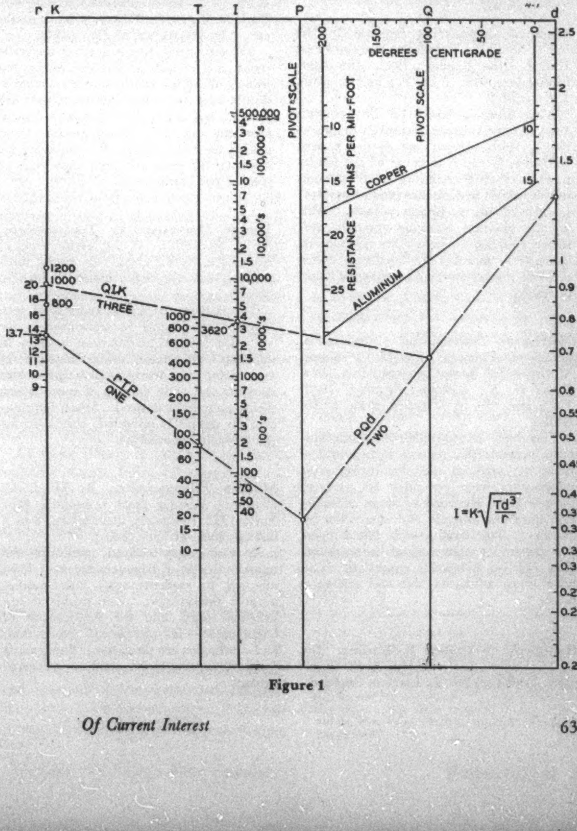


Figure 1

NEW BOOKS • • •

The following new books are among those recently received from the publishers. Books designated ESL are available at the Engineering Societies Library; these and thousands of other technical books may be borrowed from the library by mail by AIEE members. The Institute assumes no responsibility for statements made in the following summaries, information for which is taken from the prefaces of the books. All inquiries relating to the purchase of any book reviewed in these columns should be addressed to the publisher of the book in question.

Handbook of War Production. By E. A. Boyan. McGraw-Hill Book Company, Inc., New York, N. Y., London, England, 1942. 368 pages, diagrams, etc., $9^{1}/_{2}$ by 6 inches, cloth, $3. (ESL).

Discusses the problems of management that are involved in the conversion of plants to the production of war materials, and in increasing the quality and speed of output. It is based upon the experience of pioneer war manufacturers. Among the subjects discussed are the procurement of contracts, materials and supplies, production planning and control, quality control, labor and expansion, conservation of strategic material, industrial accounting in wartime, estimating for war contracts, and subcontracting.

Electrical Engineering (volume 2). By W. T. Maccall. University Tutorial Press, Ltd., London, England, 1942. 463 pages, illustrations, etc., 9 by $5^{1}/_{2}$ inches, cloth, 15s. (ESL).

This book is based on the author's "Alternating Current Electrical Engineering." With volume one of the present work it is intended to cover all the fundamentals of all branches of electrical engineering dealt with in the usual three-year courses given in British schools. This volume discusses symbolic notation, harmonic analysis, a-c generators and motors, converters, mercury-arc rectifiers, transmission, protection, and symmetrical components.

Microwave Transmission (International Series in Physics). By J. C. Slater. McGraw-Hill Book Company, Inc., New York, N. Y., London, England, 1942. 309 pages, $9^{1}/_{2}$ by 6 inches, cloth, $3.50. (ESL).

This book on ultrahigh-frequency systems presents the general theory underlying the methods used for transmitting microwaves from generator to receiver, including the intermediate stage of radiation from one antenna and absorption by another. The treatment is based upon the theory of conventional transmission lines and on Maxwell's equations. The book is an advanced text and reference book.

Handbook of Applied Hydraulics. By C. V. Davis. McGraw-Hill Book Company, New York, N. Y.. London, England, 1942. 1,084 pages, illustrations, 9 by 6 inches, $7.50. (ESL).

A general reference work on hydraulics, composed of brief, yet complete texts on its various branches with practical information on the planning and design of hydraulic works. Hydrology, river regulation, dams, spillways, canals, hydroelectric plants hydraulic machinery, water supplies, sewerage, irrigation, and drainage are discussed by eighteen prominent engineers with experience in various fields.

Natural Trigonometric Functions (to Seven Decimal Places for Every Ten Seconds of Arc). By H. C. Ives. 2d edition, John Wiley and Sons, New York, N. Y., Chapman and Hall, London, England, 1942. 351 pages, diagrams, etc., 10 by 7 inches, cloth, $9. (ESL).

These tables give the natural sines, cosines, tangents, and cotangents. They also include eleven other tables frequently needed by surveyors. Errors in the previous edition have been corrected, and a table giving the tangents and cotangents to single seconds from 0 to 2 degrees have been added.

Basic Radio, the Essentials of Electron Tubes and Their Circuits. By J. B. Hoag. D. Van Nostrand Company, New York, N. Y., 1942. 379 pages, illustrations, etc., 9 by $5^{1}/_{2}$ inches, $3.25. (ESL).

The aim in this text is to select the radio tubes and circuits which experience has proved useful, to present a simple explanation of how they work and where they are applied, and to provide sufficient numerical constants and other details to make them readily understandable. It is intended for students with only a limited knowledge of physics and mathematics.

Torque Converters or Transmissions. By P. M. Heldt. P. M. Heldt, Nyack, New York, N. Y., 1942. 406 pages, illustrations, etc., $8^{1}/_{2}$ by 5 inches, cloth, $4. (ESL).

The past ten or fifteen years have seen many developments in transmissions for use with internal combustion engines in vehicles. This book brings together in convenient form for use by designers and students the wide variety of mechanisms which have been devised. Much information on design is provided, especially for the types in common use.

Modern Trigonometry. By M. J. G. Hearley. Ronald Press Company, New York, 1942. 168 pages, illustrations, 8 by 5 inches, cloth, $1.75. (ESL).

An elementary textbook presenting the numerical side of trigonometry in a form adapted to students with little mathematical background. The problems are practical ones, and the applications of trigonometry in astronomy, navigation, and mechanics are discussed. The book is designed especially for students of aeronautics.

The Steam Locomotive. R. P. Johnson. Simmons-Boardman Publishing Corporation, New York, N. Y., 1942. 502 pages, illustrations, $9^{1}/_{2}$ by 6 inches, cloth, $3.50. (ESL).

Discusses certain fundamentals of locomotive theory and operation. In addition, the economics of the steam locomotive are considered and comparison with the Diesel-electric variety is made. The book contains much of interest to designers and those concerned with railroad motive power.

Arc-Welding Job Training Units. Dunwoody Series Welding-Training Jobs. American Technical Society, Chicago, Ill., 1942. 103 pages, illustrations, etc., 11 by $8^{1}/_{2}$ inches, paper, $1.25. (ESL).

Presents a course in welding with the electric arc adapted to the needs of industrial schools and apprentice training. The course consists of forty jobs, an information sheet being furnished for each. Question sheets are also provided for most jobs. The course has been thoroughly tested in schools and industries.

Postwar Planning in the United States. By G. B. Galloway. Twentieth Century Fund, New York, N. Y., 1942. 158 pages, tables, 9 by 6 inches, paper, 60 cents. (ESL).

This report summarizes the activities, personnel, and publications of the various agencies engaged in research upon the economic and social problems that will face us when the war ends. A considerable bibliography is appended.

PAMPHLETS • • •

The following recently issued pamphlets may be of interest to readers of "Electrical Engineering." All inquiries should be addressed to the issuers.

Patents and Invention. National Association of Manufacturers, New York, N. Y., 1942. 20 pages, no charge for single copies.

Post-War Agenda. National Resources Planning Board, Washington, D. C., 1942. Outline chart.

Standards for Foam Extinguishing Systems. National Board of Fire Underwriters, New York, N. Y., 1942. 31 pages, $1.50 per hundred, $10 per thousand copies.

Ohio Stream Drainage Areas and Flow Duration Tables. C. H. Wall, C. V. Youngquist. (The Engineering Experiment Station Bulletin 111, Ohio State University Studies, Engineering Series, Columbus, 1942.) 73 pages, 75 cents.

Voluntary Labor Arbitration Rules of Procedure. Revised edition. American Arbitration Association, New York, N. Y., 1942. 35 pages, no charge.

TRANSACTIONS SECTION

Preprint of Corresponding Pages From the Current Annual AIEE Transactions Volume

Any discussion of these papers will appear in the December 1942 Supplement to Electrical Engineering—Transactions Section

Energy Flow in Electric Systems—the *Vi* Energy-Flow Postulate

JOSEPH SLEPIAN
FELLOW AIEE

Synopsis: The conditions which a valid postulated electric-energy flow must satisfy are given and are stated to be insufficient for its unique determination. The commonly used *Vi* energy-flow postulate is shown by examples to be not generally valid, but by adding a simple term it can be made equally valid with other valid energy-flow postulates. Various examples are given of the application of this corrected energy-flow postulate. On power systems the engineer commonly limits his use of the uncorrected *Vi* postulate to applications where the correcting term should have a negligible net effect. Various examples of such use are discussed.

I. Introduction

Among power electrical engineers the following postulate as to electric-energy flow is extensively used. Let V be the electric potential at any particular point, referred to some arbitrarily chosen point of zero potential. Let i be the current density at that point. V, of course, is a scalar quantity, having magnitude but no associated direction in space; i is a vector, having magnitude and also direction in space. The product Vi is a vector and is postulated to describe or represent the density of a flow of electric energy in watts per square centimeter, V being in volts and i in amperes per square centimeter. In particular, the point in question may be a point in the cross section of a linear conductor or cable. The postulated electric-energy-flow density may be integrated over the section of the conductor to give the total postulated energy flow along the conductor. If, as is usually the case, the potential V is constant over the section of the conductor, then the total postulated energy flow will be VI, where I, a vector, is the total current flowing over the section of the conductor. This will be the reading of an instantaneous wattmeter connected into and to the conductor at that point. The average in time of this quantity VI will be the reading of the usual wattmeter at that point.

What are the phenomena which can be actually observed which justify or make valid the energy-flow postulate, if it is valid? They are the following. At some points in space electric energy is being generated or created. By that is meant that energy of some other well-known, recognizable, and measurable form is disappearing, and at the same time electric manifestations are taking place there, such as the flow of currents, the appearance of electric fields, and so forth. Thus, in a generator chemical energy of a pile of coal is disappearing, or potential energy of water in a reservoir is lessening in amount. In a discharging battery chemical energy is disappearing. In a thermocouple heat energy at the hot junction is disappearing at a rate greater than the heat energy appearing at the cold junction.

At other points in space recognizable and measurable forms of energy are appearing or being created, with simultaneous electric manifestations. In an electric oven heat is appearing in measurable amount. In an electric motor, mechanical energy is appearing as, perhaps, in the increasing potential energy of a rising elevator. In an electrochemical plant the chemical energy stored in matter is being increased.

Also, at various points in space electric and magnetic fields make their appearance, and it is found necessary, if the law of conservation of energy is to remain true, to assign a stored electromagnetic energy to these fields. It is commonly said: "It takes work to produce these fields." Where, as in the core of a reactor, there is a magnetic field, it is customary to assign a stored energy per cubic centimeter of amount $(10^{-7}/8\pi)H\cdot B$ joules where H is the magnetic intensity, and B is the magnetic induction in gauss. Where, as in the dielectric of a capacitor, there is an electric field, it is customary to assign a stored energy per unit volume of $^1/_2\,E\cdot D$ where E is the electric intensity in volts per centimeter, and D is the electric induction in coulomb-centimeters. In empty space, these assigned stored energy densities are respectively $\frac{10^{-7}}{8\pi}H^2$, and $\frac{1.11\cdot 10^{-12}}{8\pi}E^2$ joules per cubic centimeter. If these energy densities are integrated over all space, they give correctly the total energy which must be regarded as stored in the electromagnetic field if the law of conservation of energy is to remain true.[*]

At the various points of space this stored electromagnetic energy will be appearing or disappearing accordingly as the electric and magnetic fields are increasing or decreasing in intensity.

The postulated energy flow may be said to be substantiated, made valid, justified, or established if it properly co-operates with or fits in with these observable phenomena which have just been described. Where electric energy is being generated, the postulated energy flow must show energy flowing away and in proper amount. In regions where electric energy is being consumed, the postulated energy flow must make energy approach in proper amounts. At places where electric and magnetic fields are changing, and with them the associated stored energy, the postulated energy flow must make approach or recede the proper amounts of energy.

Paper 42-141, recommended by the AIEE committee on basic sciences for presentation at the AIEE summer convention, Chicago, Ill., June 22–26, 1942. Manuscript submitted February 9, 1942; made available for printing May 29, 1942.

Joseph Slepian is associate director of research, research laboratories, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

[*] It turns out that these energy densities are not the only ones which may be assigned to the various points of space, which will integrate to the correct total electromagnetic energy. See, for example, reference 1. Therefore, these stored energy densities do not have a unique validity. However, in this paper, only these assumed energy densities will be considered in their relation to postulated energy flow.

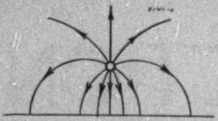


Figure 1 (left). Field E_1 in plane perpendicular to line

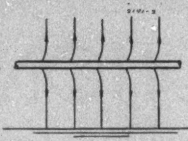


Figure 2 (right). Field E_1 in plane through line

The conditions establishing the validity of the postulated energy flow may be stated more exactly in this way. Construct a closed geometric surface anywhere in the electric system. Determine the postulated energy-flow density at every point of the surface. Integrate over the whole surface, obtaining the postulated total energy flow outward through the surface. This total energy flow out must correspond to the total changes taking place within the region enclosed by the surface. That is, the total energy flow out through the surface calculated according to the postulate must equal the excess of the rate of generation of electric energy over the sum of the rate of consumption and rate of increase of storage of electric energy within the surface.

It will be evident at once to the trained mathematician, and after a little study to the more lay engineer, that the conditions just given are not sufficient for determining uniquely an energy flow. For the postulate must merely direct the energy from the various generation points to the various consumption points, in their totality; which generation point is to feed which consumption point, and by what route, is completely undetermined so far as concerns any phenomenon which can be actually physically observed. Infinitely many postulates may be devised which will all be equally valid and equally well established by the conditions which have been described, and which are the only conditions available for defining an energy flow.

Actually, a postulated energy flow other than the *Vi* postulate, which is the subject of this paper, is very widely used, and not merely in abstract theory but for practical calculations, by radio engineers and physicists. This is the Poynting vector postulate about which there is an extensive literature.† We will not stop for a description of this Poynting vector postulate in detail at this stage. Suffice it to say that it is in its details completely different from the *Vi* postulate. In general, in an electric system it will lead energy from any particular generation point to a consumption point other than that to which it will be led by the *Vi* postulate, and where it leads energy from the same generation point to the same consumption point, it will in general do so by a different route.

Calling attention to the lack of uniqueness of the validity of any and all valid energy-flow postulates is not however the primary purpose of this paper. It is rather to examine the *Vi* postulate and to determine whether it in itself is valid or established in the only physically verifiable sense which has been just described. The completely general validity of the Poynting vector postulate has been established (with the formulas for stored electromagnetic energy given above) by mathematical derivation from Maxwell's equations. The validity of the *Vi* postulate can then be determined most conveniently by checking by well-known mathematical operations whether it is equivalent to the Poynting vector postulate in the sense already explained. This has been done in another paper.[1]

It turns out that the *Vi* postulate is not generally valid, that is, it is not generally equivalent to the Poynting vector postulate. By adding a simple term, however, a new or enlarged *Vi* postulate may be given which is generally valid. It so happens that as usually used by electric-power engineers the contribution of this added term is negligibly small. Hence, no error is committed by the use of wattmeter readings in the usual way for the usual and proper purposes by the engineers of electric-power systems.

Without going into the detailed mathematics, this paper will show how the simple *Vi* postulate fails in many simple cases. It will describe the term which must be added to make the enlarged *Vi* postulate universally valid and will show in simple cases how this term makes complete the *Vi* energy-flow picture. It will show also that this term makes a negligible contribution in the usual use of wattmeters on power systems for usual proper purposes. To do these things, however, it is necessary to examine somewhat carefully the meaning of the electric potential *V*, the current density *i*, and the nature of the electric field.

II. The Electric Potential, V

The potential difference between two points is frequently defined as the integral of the electric force from the one point to the other, or, in other words, as the work which must be done in moving a unit positive charge from the one point to the other. This is equivalent to saying that it is the reading of a suitable voltmeter connected to the two points in question.

For d-c systems, where electric and magnetic fields are steady and unchanging, this definition is satisfactory. In this case the electric-field intensity is the negative gradient of the potential so defined. In this case also the simple *Vi* postulate is generally valid.

In this d-c case, the electric field and the potential of which it is the negative gradient can be calculated from the distribution of charges. If there are no dielectrics, the potential *V* is given by

$$V=\iiint\frac{\zeta}{r}dv \qquad (1)$$

where ζ is the charge density at any variable point, r is the distance from this variable point to the point at which the potential is being determined, and the integration is carried out through all space. If dielectrics are present, then a term must be added under the integral of equation 1, giving the effect of the polarization density in the dielectrics, but for simplicity we shall leave this out of the formula, as it can be readily supplied by those readers sufficiently trained to feel the lack of it. The electric field is then given by

$$E=-\text{grad } V \qquad (2)$$

In the case of an a-c system, however, the definition of the potential given at the beginning of this section fails. The work done in moving a unit charge from one point to another is no longer uniquely determined. The work done is different according to the path chosen in going from the one point to the other. For example, the work done in moving a charge from a point near the core of a transformer to a point diametrically opposite, by a path half way round the core,

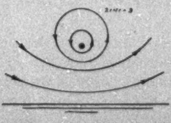


Figure 3 (left). Magnetic field around line

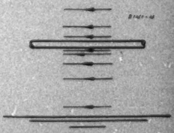


Figure 4 (right). Induced electric field E_2

---

† For description of energy flow in usual electric-power machines, according to the Poynting vector postulate, see Slepian, *Electric Journal*, volume 16, July 1919, page 303.

will be different from the work done if the charge is moved from the one point to the other in the other direction round the core. If a suitable voltmeter is connected between the two points, the reading will be different according to whether the leads go round the core by the one path or the other.

We may however define a function calculated from the observable distribution of charges (and polarizations of dielectrics) by means of equation 1. We shall call this the Maxwell scalar potential,[‡] V_M, so that

$$V_M = \int\int\int \frac{\zeta}{r} dv \qquad (3)$$

Of course, the electric field is not the negative gradient of this scalar potential

$$E \neq -\text{grad } V_M \qquad (4)$$

In fact E is not the gradient of any potential.

We may regard the electric field E as made up of a superposition of two fields, E_1 and E_2,

$$E = E_1 + E_2 \qquad (5)$$

where

$$E_1 = -\text{grad } V_M \qquad (6)$$

E_1 may be said to be the field produced electrostatically by the charges defining V_M.

If a charge is moved around in a closed path, the work done against the component field E_1 is necessarily zero, since E_1 is the gradient of a potential. If there is a net work, not zero, done by the charge in going round the closed path in the total field E, it must be the work done against the component field E_2. Thus the integral of E_2 around a closed path is in general not zero. By Faraday's law, this integral, sometimes called the induced electromotive force, is equal to $1/10^8$ times the rate of change of the enclosed magnetic flux, the units of electric field and magnetic flux being the usual volts per centimeter and maxwells.

The lines of force of the field E_2 cannot terminate on any charge, since equations 3 and 6 make E_1 bring to or from all the charges all the corresponding lines of force, thus leaving no charges for the lines of E_2 to terminate upon. The lines of force of E_2 then must form closed loops, linking varying magnetic flux. E_2 may then be said to be the electric field induced by the varying magnetic field, and to have no electrostatic character.

Mathematically the defining properties of E_1 and E_2 are as follows:

$$\text{div } E_1 = -4\pi\zeta, \ \zeta \text{ in appropriate units} \qquad (7)$$

$$\text{div } E_2 = 0 \qquad (8)$$

$$\text{curl } E_1 = 0 \qquad (9)$$

$$\text{curl } E_2 = -1/10^8 \frac{\partial B}{\partial t} \qquad (10)$$

It appears that the potential used by electric-power engineers is precisely the Maxwell scalar potential which has just been described.

For example consider a single transmission line parallel to the surface of a good conducting earth, which is taken as having zero potential. The charges on the line by themselves, if they were unvarying, would produce the field E_1 with lines of force lying nearly completely in planes perpendicular to the line, as in Figures 1 and 2. The slight curvature in the lines of force of E_1, and their inclination to the line, shown in Figure 2 somewhat exaggerated, are due to the charge density on the line decreasing from left to right, due to the decreasing "potential" of the line.

The current in the line, produces the magnetic field shown in Figure 3, the lines of magnetic force lying in planes perpendicular to the line. If the current is increasing, the magnetic field also increases and induces an electric field E_2 whose lines of force are nearly parallel to the line as in Figure 4. This field is strongest at the line, where it is nearly equal, and opposite in direction to the component of the field E_1 which is parallel to the line. The parallel component of the net field, E, at the line is that required by the ohmic resistance of the line to the current it is carrying. The total field E is the sum of the electrostatic field E_1 and the induced field E_2.

To determine the potential at a point 1 in the line, the power engineer connects a voltmeter from that point to the ground immediately below. That is, he integrates the electric force along a path in the plane perpendicular to the line. Since such a path is perpendicular to E_2, he essentially integrates the field E_1 alone and, therefore, by equation 6, determines the Maxwell scalar potential, V_M, at the point 1 of the line.

To determine the potential at a second point 2 in the line, the engineer connects his voltmeter from the point 2 to the earth immediately below, and not to the earth point used for getting the potential at point 1. To find the potential difference between points 1 and 2, the engineer takes the difference between the potentials so determined. He does not connect a voltmeter from point 1 to point 2 with leads running parallel to and along the line. If he did, the field E_2 would contribute to the integral of E from 1 to 2 and he would get a result differing from that obtained by his usual procedure. He would say that this last procedure would give him only the resistance drop in the line, whereas his usual procedure gives him the total (resistance plus inductance) drop in the line. It is clear then that the power engineer uses the Maxwell scalar potential. Therefore, subsequently in this paper, the subscript M will be dropped from the V symbol for potential, it being understood that the Maxwell scalar potential is that which is meant.

III. The Corrected Vi Postulate

The appearance of the induced field E_2 is one of the features which distinguishes the variable-state or a-c system from the steady-state or d-c system. If E_2 is absent or negligibly small, then the usual energy-flow postulate,

$$P_1 = Vi \qquad (11)$$

is generally valid, provided i includes displacement currents as well as conduction currents, as will be explained in the next section. If however, E_2 is not absent, equation 11 becomes invalid. Then the following energy-flow postulate,

$$P_2 = Vi + \frac{10}{4\pi}[E_2 \times H] \qquad (12)$$

is generally valid. The bracket symbol in equation 12 indicates the vector product of E_2 and H, whose meaning will be explained later in examples. The two terms in P_2 may be called respectively the conductive and inductive components of the postulated energy flow.

IV. The Current Density i—Example of Variable-State System Where P₁ Is Valid

To see how the inclusion of displacement currents in i makes P_1 valid as long as E_2 is absent, consider the following example:

A sphere of radius R, far removed from other objects, is charged to potential V_o. It is then surrounded by a spherically symmetrical radial electric field, with $V = V_o R \frac{1}{r}$, and $E = V_o R \frac{1}{r^2}$ where r is the distance from the center. There will then be a stored energy density, $T_e = \frac{1.11 \cdot 10^{-12}}{8\pi} E^2$ $= \frac{1.11 \cdot 10^{-12}}{8\pi} V_o^2 R^2 \frac{1}{r^4}$ joules per cubic centimeter in the space around the sphere.

[‡] Physicists now more generally use the retarded scalar potential of Lorentz. Except for very long lines in power systems, or high frequencies, this potential will differ very slightly from the scalar potential of Maxwell, which is used here because of its simpler description.

Now let the sphere be joined by a fine high-resistance wire to the remote ground. The sphere slowly discharges by the conduction current through the wire. The small current flowing produces a magnetic field, but let us suppose it is so small that we may neglect the stored energy which is associated with it and also the small induced field E_2 which it produces by its slow change.

The stored energy in the electrostatic field slowly disappears, and a corresponding amount of heat appears in the wire. The valid energy-flow postulate, $P_1=Vi$ must show energy flowing from the space where it is initially stored to the wire where it appears as heat.

In the wire itself, let the total conduction current be I. At a point where the potential is V_a, the postulate P_1 asserts that there is an energy flow in the wire equal to V_aI watts away from the sphere. At a more remote point on the wire, where the potential is V_b, the energy flow in the wire is V_bI, watts away from the sphere. V_bI is less than V_aI, the difference being $(V_a-V_b)I$. But if ζ_{ab} is the resistance of the wire in ohms between points a and b, $V_a-V_b=\zeta_{ab}I$, and $(V_a-V_b)I=\zeta_{ab}I^2$ which is precisely the joulean heat developed in the wire between a and b. Thus the postulate $P_1=Vi$ shows energy flowing from the sphere down the wire where it appears as heat at the proper places and in the proper amounts.

But the valid postulate $P_1=Vi$ must also show how the energy stored in the electrostatic field arrives at the sphere so that it may be directed thence into the wire. The displacement current density comes to the rescue for this purpose.

Where an electric field E is varying, in free space, a displacement current density is defined as $i=\frac{1.11\cdot 10^{-12}}{4\pi}\frac{\partial E}{\partial t}$ amperes per square centimeter. In the present example E is decreasing so that there is a displacement current density flowing inwards towards the sphere given by $i=\frac{1.11\cdot 10^{-12}}{4\pi}R\frac{dV_o}{dt}\frac{1}{r^2}$. The total displacement current flowing inwards over the surface of a sphere of radius r_a will be $4\pi r_a^2 i=1.11\cdot 10^{-12}R\frac{dV_o}{dt}$. The potential at r_a being $RV_o\frac{1}{r_a}$ volts, there is a total energy flow inwards across the surface, according to the Vi postulate, of $1.11\cdot 10^{-12}R^2V_o\frac{dV_o}{dt}\;\frac{dV_o}{dt}\frac{1}{r_a}$ watts. Across a more remote sphere, of radius r_b, the total energy flow inwards is $1.11\cdot 10^{-12}R^2V_o\times\frac{dV_o}{dt}\frac{1}{r_b}$. The flow inwards across the inner sphere exceeds the flow inwards across the outer sphere by $1.1\cdot 10^{-12}R^2V_o\frac{dV_o}{dt}\times\left(\frac{1}{r_a}-\frac{1}{r_b}\right)$ watts. This should be equal to the rate of decrease of the energy stored in the shell between the two spherical surfaces.

But integrating the stored energy density, $T_e=1.11\cdot 10^{-12}V_o^2R^2\frac{1}{r^4}$ over this spherical shell, we get $\frac{1.11\cdot 10^{-8}}{2}\;V_o^2R^2\times\left(\frac{1}{r_a}-\frac{1}{r_b}\right)$ joules. The rate of change of this energy, $1.11\cdot 10^{-12}V_o\frac{dV_o}{dt}R^2\left(\frac{1}{r_a}-\frac{1}{r_b}\right)$ checks exactly with the assertion of the Vi postulate in the preceding paragraph.

Figure 5 shows diagrammatically this flow of the stored energy to the sphere and thence to the wire according to the Vi postulate. For comparison, Figure 6 shows the energy flow according to the Poynting vector postulate.

V. Example of Failure of Simple Vi Postulate

Consider a long straight linear conductor of circular section and radius R. Let a current flow in it which is increasing at a constant rate, $I=I_0t$. Let the return circuit be the parallel-plane good conducting ground a considerable distance below the conductor.

There will be a magnetic field surrounding the conductor as in Figure 3, and this field will be increasing in strength at a constant rate. The stored energy associated with this field and distributed in the space will also be increasing. A valid energy-flow postulate must show a flow of energy into these regions where the stored energy density is increasing.

A potential drop (Maxwell scalar potential drop) will exist along the line, called by the engineer the inductive drop along the line. Neglecting the ohmic resistance of the line this potential drop will be constant in time. Associated with this distribution of potential on the line will be an electrostatic field E_1 as pictured in Figure 2. This field will be constant in time.

The steadily increasing magnetic field of Figure 3 will induce an electric field E_2 as pictured in Figure 4. This induced field E_2 is also constant in time.

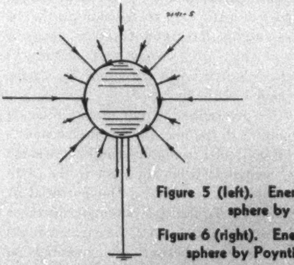
Let us apply the simple $P_1=Vi$ postulate to this case. It shows an energy flow in the line at each point of an amount VI watts, where V is the potential at the point. Everywhere else, other than in the line, the current density is zero. There is no displacement current in the space because the electric fields E_1 and E_2 are constant in time. The simple $P_1=Vi$ postulate then shows energy flowing in the line and nowhere else.

Consider a short length of the line. Because the potential at the entering end is larger, the energy flow entering the section is greater than that leaving the section. Hence, if the simple Vi postulate is valid, there should be appearing in the section an increasing amount of some form of energy, such as heat for example. But no such energy appears there! The simple Vi postulate fails!

Likewise, out in space the stored magnetic energy is increasing. The Vi postulate should show an energy flow bringing this stored energy to its proper position in space. It does not! It fails!

VI. Success of the Enlarged Postulate, $P_2=Vi+\frac{10}{4\pi}[E_2\times H]$, With Preceding Example

The bracket in the second or inductive term of the enlarged postulate, P_2, denotes the vector product of the induced electric field E_2 and the magnetic field H. This is a vector which stands perpendicular to both E_2 and H, with direction given by the right hand rule, that is, the direction of advance of a right-hand screw when



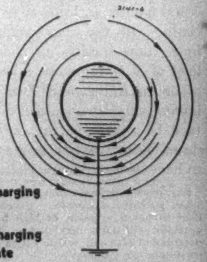


Figure 5 (left). Energy flow for discharging sphere by Vi postulate

Figure 6 (right). Energy flow for discharging sphere by Poynting vector postulate turned in the sense which brings E_2 most quickly into line with H. Its magnitude is equal to the product of the magnitudes of E_2 and H, respectively, and the sine of the angle between them.

Let the induced electric field E_2 have the value E_{20} at the conductor itself. The (Maxwell scalar) potential gradient along the conductor will have the same magnitude as E_{20}, since we are neglecting the ohmic resistance of the line, but will be oppositely directed.

Let the length of the portion of the line we are considering be l centimeters. The potential drop through this portion will then be $l|E_{20}|$, $|E_{20}|$ being the magnitude of the vector E_{20}. The first or conductive term of P_2 then will give an excess of energy entering the one end of the line portion over that leaving the other end by an amount equal to $l|E_{20}|I$ watts.

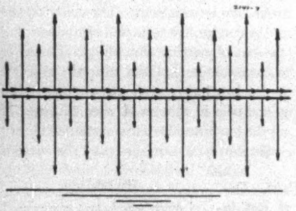


Figure 7. Energy flows about line carrying increasing current by P_2 postulate

At the surface of the line, the magnetic field H_o is perpendicular to the line and E_{20} and has the magnitude $|H|_o=\frac{2I}{10R}$. The second term of P_2 then shows an energy-flow density leaving the conductor surface of $\frac{10}{4\pi}|E_{20}|\frac{2I}{10R}=\frac{1}{2\pi}|E_{20}|\frac{I}{R}$ watts per square centimeter. Multiplying by the area of the surface of the line, $2\pi Rl$, we find the second term showing an energy flow out of the line of an amount $l|E_{20}|I$ watts. Comparing with the preceding paragraph we see that the enlarged postulate P_2 calls for the appearance in the line of heat or other energy of amount zero! In the line P_2 succeeds!

In the space outside the line, while the first term of P_2 shows zero energy flow, the second or inductive term shows energy flow radially outwards into space. It is not difficult to show that this radial flow is of just the proper amount to account for the increasing magnetic energy stored there. Figure 7 shows diagrammatically the energy flow about the line according to P_2.

P_2 succeeds everywhere! It must, since it has been shown in the paper of footnote 1 that it is equally valid with the Poynting vector postulate.

VII. Energy Flow in Transformer

It is instructive to trace the flow of energy in a transformer according to the simple and enlarged *Vi* postulates. The simple *Vi* postulate fails, of course. The enlarged postulate succeeds because of its inductive term.

In each turn of the primary of the transformer, there is a potential drop equal to the induced voltage per turn, V_t, if the resistance drop is neglected. According to the simple *Vi* postulate then, more energy flows into the turn than out by an amount V_tI_p watts, where I_p is the primary current. The simple *Vi* postulate then makes $V_tn_pI_p$ (n_p = number of primary turns) watts disappear in the primary, apparently causing energy to be annihilated.

Similarly, the simple *Vi* postulate calls for the apparent creation of energy in the secondary at the rate $V_tn_sI_s$ watts.

The second term of the enlarged *Vi* postulate avoids this apparent distressing destruction of energy in primary and creation of energy in secondary by providing an energy flow from primary to secondary.

Between the primary and secondary coils there will be a magnetic field perpendicular to the coil wires, Figure 8. This field will have magnitude approximately $|H|=\frac{4\pi}{10}\frac{n_pI_p}{l_1}$ where l_1 is the length of the coil. Between the coils there will also be an induced electric field E_2 with lines of force remaining parallel to the coil wires and perpendicular to H. The magnitude of E_2 will be approximately $|E_2|=\frac{V_t}{l_2}$ where l_2 is the perimeter of a turn.

According to the second term of P_2, then, there is an energy flow from primary to secondary of $\frac{10}{4\pi}[E_2\times H]=\frac{V_tn_pI_p}{l_1l_2}$ watts per square centimeter. Multiplying by l_1l_2, the total area carrying this energy-flow density, we get $V_tn_pI_p$ watts carried across from primary to secondary by the second term of P_2, which is just the energy brought into the primary by the first term *Vi*.

VIII. Relationship Between Poynting Vector Postulate and P₂

The Poynting vector postulate asserts the presence of an electric-energy-flow density given by

$$P=\frac{10}{4\pi}[E\times H]\text{ watts per cm}^2 \tag{13}$$

It differs from the enlarged *Vi* postulate, P_2 in making all the energy flow inductive, and none conductive.

This suggests that the resolution of the energy flow into a conductive and inductive component as in P_2 is largely arbitrary. This is true.

Let V' be any *arbitrary* scalar function of position in space. Let E_1' be a vector function of space defined by

$$E_1'=-\text{grad } V' \tag{14}$$

Let E_2' be defined by

$$E=E_1'+E_2' \tag{15}$$

Then it is readily shown that the energy flow postulate

$$P_2'=V_i'+\frac{10}{4\pi}[E_2'\times H] \tag{16}$$

is equally valid with P and P_2.

This bears out the statement made in the introduction that infinitely many valid energy-flow postulates may be devised.

The Poynting vector P, and the corrected *Vi* postulate P_2, are special cases of equation 16, obtained by respectively taking $V'=0$, and taking for V' the electric potential V.

IX. Validity of Simple Vi Postulate for Usual Proper Purposes on Power System

The power-system engineer postulates an energy flow in his lines given by connected wattmeter readings and with this postulate carries on his usual technical and commercial operations. This postulate is identical with the simple *Vi* postulate (except for the omission of displacement currents) which we have seen is not generally valid. The postulate can be made valid by the addition of the inductive term of P_2. Hence, since the engineer is successful in his usual operations, we conclude that he limits himself to those applications where the contribution of the inductive term of P_2 is negligible.

We have also seen that even the valid enlarged *Vi* postulate, P_2, is not uniquely valid. Hence, again since the engineer is successful in his usual operations, we conclude that he normally limits himself to those questions for which all valid postulated energy flows will give the same

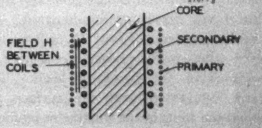


Figure 8. Magnetic field in transformer

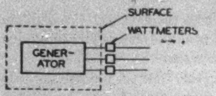


Figure 9 (left). Energy of generator by wattmeters

Correct connection

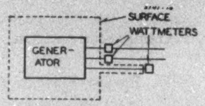


Figure 10. Energy of generators by wattmeters

Incorrect connection

answer. In referring back to the introduction, there is only one question to which all valid energy-flow postulates give the same answer, namely, the following: In a given region of space, in which there may be any and various kinds of electric apparatus, what is the excess of the total electric-energy generation over the sum of the total energy consumption and the increase in electric-energy storage?

Before giving examples, it is well to point out one use of wattmeter readings which is frequently made and which does not involve the energy-flow postulate at all. It is for determining the loading or heating of lines.

Now, the heating of a line certainly does not depend on any so intangible and intrinsically indeterminable a thing as a postulated energy flow. The heating is determined only and entirely by the current through it, and this is measured unequivocally by an ammeter. However, from the wattmeter reading the engineer, if he knows the usual voltage and power factor of the line, can make an estimate of what the current may be and thus estimate the loading or heating of his line. But in doing this the engineer is really using the wattmeter, together with his other knowledge of the line, as a kind of devious ammeter. Regardless of how he may colloquially refer to energy or power or the line, he knows that the heating of the line is determined by the current magnitude only.

As a first example, consider the determination of the energy generated by a generator. The engineer places wattmeters in all the leads coming from the generator, adds their readings algebraically, and asserts that number so obtained is the desired generation. He will be right if the inductive term of P_2 makes no contribution.

Surround the generator by a closed surface passing through the points in the leads where the engineer connected his wattmeters (Figure 9). The validity of P_2 ensures that, when integrated over this closed surface, it will give the net rate of energy generation in the generator since the energy stored remains constant. The conductive term, Vi, of P_2, will be zero everywhere in the surface except over the sections of the generator leads. This term integrated over the surface will equal the sums of the wattmeter readings.

E_2 and H which enter into the inductive term, $\frac{10}{4\pi}[E_2 \times H]$ of P_2, will each be negligibly small on the surface, except in the neighborhood of the leads. There, as explained in sections *V* and *VI* and shown in Figure 3, E_2 is parallel to the leads and therefore perpendicular to the surface. H however will lie parallel to the surface. Hence $\frac{10}{4\pi}[E_2 \times H]$ lies entirely parallel to the surface and makes zero contribution to the total energy crossing the surface by the P_2 postulate. Hence P_2 integrated over the surface equals the sum of the wattmeter readings, and the engineer's usual use of wattmeters for determining the energy generation of a generator is justified.

It is clear from this discussion that the validity of the use of wattmeters alone in the preceding example depends on the points of attachment of the wattmeters to the leads lying in a surface which is perpendicular to the E_2 field. The engineer is aware of this. If one of the wattmeters is displaced along one of the leads from its proper position, as in Figure 10, the engineer knows that the sum of their readings will no longer equal the rate of energy generation in the generator. Ordinarily he will say that this is due to the inductive action of the leads, each on the other. This paper describes this situation by observing that the inductive term of P_2 in this case asserts a flow of energy not zero over the portion of the enclosing surface, which lies parallel to the leads.

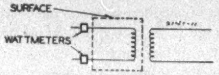


Figure 11 (left). Determination of loss in primary of a transformer

Another example: The engineer will not attempt to determine the losses in the primary coil of a transformer by means of wattmeters alone. In referring to Figure 11, showing a transformer diagrammatically, it is clear that for a surface which encloses the primary alone, there will necessarily be some portions across which the inductive term of P_2 will also assert there is an energy flow, as explained in section VII, and this energy flow must be added algebraically to the wattmeter readings to obtain the primary losses. However, the engineer will not hesitate to determine the total losses in a transformer alone, since in that case an enclosing surface may be drawn such that across it the inductive term of P_2 will assert zero energy flow.

A last example: Two power systems, *A* and *B*, operate with an interconnecting transmission line, in which wattmeters are properly placed. The engineer advises that the owners of these systems make payments to one another depending on the sum of the readings of these wattmeters. What is the physical basis on which the engineer makes his recommendation?

Referring to Figure 12, a surface may be drawn through the points of connection of the wattmeters and enclosing completely the system *A*, such that the contribution to the energy flow across the surface according to postulate P_2 of the inductive term is zero. The contribution of the conductive term will of course equal the sum of wattmeter readings. Hence we may conclude that the sum of the wattmeter readings equals the excess of the generation in system *A* over the sum of all the loads and losses in system *A*.

Similarly, a surface may be drawn through the wattmeter connections and enclosing the system *B*, which will justify the statement that this same sum of the wattmeter readings also equals the excess of the sum of the loads and losses over generation in system *B*.

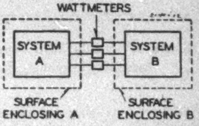


Figure 12. Power systems interconnected

Because of this numerical relationship between the wattmeter readings and the balance between generation, loads, and losses on the two respective systems, the engineer makes his recommendation that the owners of system *B* compensate the owners of system *A*. The engineer may in addition make some irrelevant assertions concerning energy flow in the transmission line, while a physicist may equally irrelevantly speak of an energy flow from *A* to *B* through space.

The owners of the systems are indifferent to these reflections of engineer and physicist. Their esthetic tastes may perhaps be more like that of the engineer so that they may prefer an energy flow postulate P_2 which makes some of the energy (but not all) flow in wires, to one like P, where nearly all of the energy flows in space and quite remotely from the wires.

Power-System Interconnection in Quebec

W. R. WAY
MEMBER AIEE

I. Introduction

THE main purpose of this paper is to describe the development of a major interconnected system located in the province of Quebec.

By means of a study, based on prewar data when stable conditions existed, and by suitable illustrations and explanations, it is aimed to show how it has been possible, through interconnection of three important hydroelectric systems, to increase the combined firm power capacity far beyond the sum of the capacities of the individual systems if operated independently. It will be brought out that the diversity in load characteristics and hydraulic conditions makes it feasible to accomplish this result and to effect other desirable economies on these systems, which are dependent solely on power supply from hydroelectric plants.

The possibilities of system consolidation had been under study for some years, but the war, with the urgent demand for more firm power, forced the issue to a conclusion, involving combined power resources of nearly 4,000,000 horsepower as indicated in Table I.

Reference will also be made to certain engineering features and operating practices of these systems, which may prove to be of interest.

II. Description of Geographical Areas

In leading up to a detailed analysis of the present interconnected system, it is thought desirable to describe briefly the areas under consideration.

The Montreal Area. This area is considered as including the territory in the immediate vicinity of Montreal Island on which is located the city of Montreal. Power is supplied by the Montreal Light, Heat, and Power Consolidated principally from two power developments on the St. Lawrence River. The plants of the Montreal Island Power Company and of the Canadian Light and Power Company also feed into this area.

Originally, in the year 1900, electric service was supplied by a number of small isolated power companies, not interconnected in any way and each serving its own independent district. In most cases standby steam plants were necessary to guard against the frequent interruptions to the supply from the hydroelectric plants because of ice trouble, breakdown, and other causes.

In 1902 the Montreal Light, Heat, and Power Consolidated was formed and acquired jurisdiction over most of the isolated systems in and near Montreal. It was soon realized that for reasons of economy, and to secure better service, some consolidation of systems or interconnection between plants was essential.

While some immediate constructive steps were taken in this direction, it was found that parallel operation was difficult or in some cases impossible because of the different frequencies, these being 66, 60, and 30 cycles. Furthermore, some systems were two-phase, while others were three-phase, and even direct-current. Eventually by compromising on 63 cycles, tying in through frequency changers, increasing the 30-cycle frequency to 31.5 cycles, and adopting three-phase alternating current, a common operating base was obtained, but this did not necessarily indicate parallel operation of the combined systems. Actually it was not until 1923 that all the subsystems of the Montreal area were operated completely in parallel.

The Shawinigan Area takes in the territory on both sides of the St. Lawrence River from Montreal to Murray Bay, some 80 miles below Quebec City, and is served mainly by the Shawinigan Water and Power Company, the Quebec Power Company, and the Southern Canada Power Company.

The Saguenay Area in the Lake St. John region is served mainly from plants on the Saguenay River by companies under the control of the Aluminum Company of Canada, Ltd.

III. Load, Output, and Hydraulic Characteristics of the Individual Systems

In the *Montreal Area* the St. Lawrence River is the chief source of power and has a flow which is unusually uniform, the average mean flow being 220,000 cubic feet per second, while the maximum and minimum recorded flows are 318,000 cubic feet per second and 173,000 cubic feet per second respectively. The heads involved are from 26 to 80 feet; comparatively long headrace canals are found, with the main difficulties in power output being caused by ice jams or other forms of ice trouble, occurring during the months of January and February. A typical hydrograph is shown in Figure 1.

The nature of the load in the Montreal area is that which might be expected in any large metropolitan area, with weekday load factor in the winter months of the order of 68 per cent and an annual load factor of 47.5 per cent during the prewar period. Maximum peak loads occur in the months of November or December, with a definite tapering off during the summer months. This is well

Paper 42-150, recommended by the AIEE committees on power transmission and distribution and power generation for presentation at the AIEE Pacific Coast convention, Vancouver, B. C., Canada, September 9–11, 1942. Manuscript submitted June 4, 1942; made available for printing July 9, 1942.

W. R. WAY is assistant general superintendent of the Shawinigan Water and Power Company, Montreal, Quebec, Canada.

The author acknowledges the assistance of J. R. Hango of the Saguenay Transmission Company, Ltd., and Cooper Antliffe of the Montreal Light, Heat, and Power Consolidated in the preparation of this paper.

But what actually moves the owners of A to expect compensation is the physically observable fact that the generation in A exceeds the sum of the energies billed for at all the loads in A plus the losses in A. Likewise the owners of B are persuaded to make compensation by the physically observable fact that the total energy generated in B is less than the sum of the energies billed for at all the loads in B plus the losses in B. These same physically determinable facts as to location and magnitude of electric-energy generation and consumption, which basically are those alone in which owners of power systems are interested when engaged in "exchanges of energy," are also the only physically determinable facts on which the infinitely many valid energy-flow postulates will agree. The engineer is quite right in choosing one of them, namely $P_2 = Vi + \frac{10}{4\pi}[E_2 \times H]$, for determining the magnitude of the so-called "exchange of energy," and applying it under such a condition that the second term may be neglected, and determining the effect of the first term by wattmeter readings.

Reference

1. **Joseph Slepian.** *Journal of Applied Physics*, **volume 13, 1942, pages 512–18.**

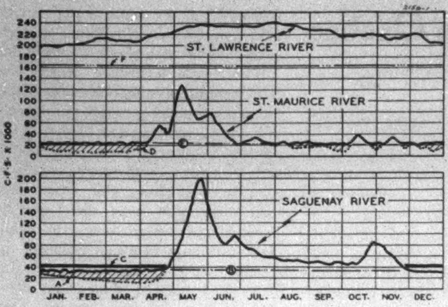


Figure 1. Hydrographs of St. Lawrence, St. Maurice, and Saguenay Rivers

A. Unregulated flow, Saguenay River

B. Regulated flow 35,000 cubic feet per second, 1942

C. Estimated regulated flow, 45,000 cubic feet per second 1943

D. Unregulated flow, St. Maurice River, 6,000 cubic feet per second, minimum

E. Regulated flow, 22,000 cubic feet per second, 1942

F. Utilization of St. Lawrence River flow, 1942—164,000 cubic feet per second

illustrated by comparing the typical daily load curves shown in Figure 2.

The chief need consequently is peak power for short periods during the winter months, while during the remainder of the year large and variable amounts of surplus energy and power are indicated, particularly during the months of April to November inclusive, with no immediate market available.

From the point of view of output, the gross primary kilowatt-hours sold in this entire area for 1941 were 3,341,902,000, while the corresponding maximum system peak load for primary power was 586,200 kw.

In March 1942, there was available 60-cycle capacity horsepower to the extent of 650,000 horsepower apart from that available from the Shawinigan Water and Power Company under the 160,000-horsepower contract.

In the Shawinigan Area the St. Maurice River is the most important source of power. It has a drainage area of 16,000 square miles and is suitably equipped for good regulation, having a number of storage reservoirs with a total storage of 13,000 square-mile-feet. This gives an indication of the extent to which the river is regulated and the possibilities of storing water for the combined system.

By using these storages, the regulated flow (Figure 1) can usually be maintained at 22,000 cubic feet per second, as compared with the unregulated minimum flow of 6,000 cubic feet per second. The maximum flow now experienced is of the order of 120,000 cubic feet per second which occurs early in May. As a result plants on this river have little difficulty in maintaining a high rate of output, excepting during the high tail-water period when in certain cases the plant output is markedly reduced. It so happens, however, that in the month of May the Montreal area is in a position to rectify this condition to a great extent, so that from the hydraulic standpoint, there is some diversity between the Montreal and Shawinigan areas.

From the load standpoint the Shawinigan system firm load is of an unusually high load factor, being of the order of 92 per cent on a week day, with an annual load factor under prewar conditions of 71 per cent. The type of firm load includes paper mills, aluminum production, carbide and carborundum furnaces, and asbestos mills, as well as a few steel plants.

Until recently, on account of the important paper-mill load which required the supply of quantities of process steam, large amounts (maximum peak over 500,000 horsepower) of surplus energy were supplied to electric boilers to replace coal-fired steam at the mills. This surplus was not only that obtainable from the Shawinigan area proper but also included secondary power purchased from neighboring companies.

Output characteristics of the Shawinigan system during the year 1941 included gross kilowatt-hour sales of 6,270,760,543 kilowatt-hours with a maximum 60-cycle firm peak of 868,000 kw. In March 1942 this area was served by 30-cycle and 60-cycle capacity to the amount of 1,154,500 horsepower aside from the 100,000-horsepower firm power contract with the Saguenay Power Company.

In the *Saguenay Area*, the Saguenay River is fed from a watershed area of 28,100 square miles. The hydrograph of this river (Figure 1) is somewhat similar to that of the St. Maurice River but is fortunately different as to the time of occurrence of the high water period, thus indicating advantages from interconnection with the St. Maurice River plants.

Prior to 1941 no storage reservoirs on this watershed had been constructed for regulating the flow of the Saguenay River, but Lake St. John itself (6,080 square-mile-feet usable storage) is utilized to the greatest practical extent from December to March inclusive.

Regarding load characteristics: before the war, power developed in the Saguenay area was utilized principally for the production of paper and aluminum and to supply the 100,000-horsepower firm power contract with the Shawinigan Water and Power Company. During periods of excess energy, the surplus was utilized for electric steam generation, the output for this purpose attaining at times the value of 375,000 horsepower. Since 1940, on account of the tremendous increase in demand for firm power, the electric boiler load has been discontinued.

The gross output of the plants in the Saguenay area during 1941 was 4,538,000,000 kilowatt-hours with a maximum firm peak of 639,000 kw. To serve this area, as in March 1942, there was capacity normally available to the extent of 1,000,000 horsepower.

IV. Possibilities of Maximum Utilization of Resources by Interconnection

In order to illustrate how the differences in the system characteristics of the three areas, could be utilized fully, a study has been made and a series of curves (Figure 3) prepared to show the peak (kilowatt) possibilities, while the kilowatt-hour problem is set out in Table II. The data have been based on *normal prewar conditions in 1939*, when interconnection was being considered.

The system in each area is analyzed graphically (Figure 3) to show its maximum available generator capacity (*G*),

Table I. Output Data of Plants Associated With Interconnection

	Total Rated Turbine Output (Horsepower)	Size of Largest Unit (Horsepower)	Number of Units
Montreal area supplied chiefly by Montreal Light, Heat, and Power Consolidated	968,000	53,000	45
Shawinigan area supplied chiefly by Shawinigan Water and Power Company	1,089,500	44,500	48
Saguenay or Lake St. John area supplied chiefly by Aluminum Company of Canada, Ltd.	1,850,000	85,000	28
Miscellaneous Companies	80,000	6,000	20
Total	3,987,500 horsepower		

Maximum rated capacity operated in parallel (2,586,000 horsepower) 1,930,000 kw.

power available (*P*) as influenced by hydraulic conditions, and maximum firm load (*L*) which could be carried if operated as a completely independent system. To simplify the study, no allowance for reserve has been made to take care of load growth or frequency regulation.

From Figure 3A it may be noted that in the *Montreal area*, the comparatively steady power output from the St. Lawrence River plants having no storage facilities and the metropolitan load of low load factor are not an ideal combination from the point of view of matching available power facilities and load requirements. In the summer months, even at time of peak load, over 100,000 kw surplus output is indicated, while an additional 100,000 kw is available for about seven hours each day after midnight. This unfavorable situation results in surplus energy being available to an amount of over 800,000,000 kilowatt-hours per year (item 1, Table II; see also Figure 2).

In the *Shawinigan Area*, with its large storages and regulating possibilities on the St. Maurice River, it is possible to convert the entire available annual energy into firm load irrespective of daily or seasonal requirements (item 2, Table II). The curves (Figure 3B) also show that the maximum feasible firm load that may be carried at the normal 71 per cent yearly load factor is also just equal to the limit set by reduced system generator capacity during the high tailwater period.

Under these conditions, except for two months during the spring flood period, there is a minimum of about 60,000 kw reserve generator capacity. This reserve is admirably suited to assist in converting daily or seasonal surplus energy of adjacent areas into firm power by storing such surplus energy in the St. Maurice River storage reservoirs and returning it as firm power during the winter months to satisfy peak requirements. Additional generator capacity on the St. Maurice River would permit extension of this scheme.

In the *Saguenay Area*, using average runoff for the $4^1/_2$ winter months, the maximum possible firm load to be carried is dependent on the fixed storage capacity of Lake St. John. This results in a large amount of idle generator capacity during the winter months (Figure 3C) and large surplus summer energy (item 3, Table II).

The idle generator capacity during the winter months, combined with Lake St. John storage facilities, is useful in assisting in the conversion of surplus night energy in the Montreal area into firm power. Furthermore, part of the summer surplus energy may be stored in the reservoirs of the St. Maurice River and taken back during the winter months and thus be converted into firm power.

These curves indicate that if each of the systems in the three areas were to be operated independently:

(*a*). The firm loads which each system could carry successfully would be limited definitely to the values indicated by the load curves (see Figures 3A, 3B, 3C), on account of limiting local conditions.

(*b*). The equivalent energy (kilowatt-hours) consumed by such individual firm loads would be restricted to the values given in Table II.

Such isolated system operation would leave a fair portion of the combined total resources unused, or inefficiently used, as indicated by the following:

(*a*). 1.7 billion kilowatt-hours of idle energy (from a firm-power viewpoint) equal to over 18 per cent of the total energy available. This corresponds to 260,000 kw (74.6 per cent load factor) of unused potential firm power (Table II).

(*b*). Spare generator capacity and spare power could be made available to all areas at their critical periods during the year, because of the diversity in individual system power resources and load characteristics (Figure 3D).

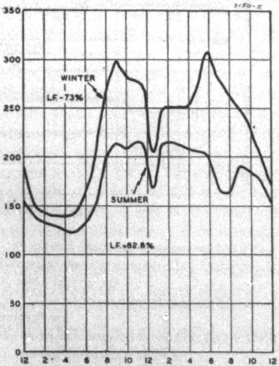


Figure 2. Typical summer and winter weekday (1941) firm load curves, Montreal area

Taken in combination with steady output of St. Lawrence River plants, the large amounts of surplus energy available for conversion into firm power are apparent

These unused resources illustrate what potential possibilities a flexible interconnection of the three systems would offer for complete utilization of combined resources.

While it is not represented that this study is complete, and without attempting to describe in detail any specific coordination scheme or outline its practical execution, which would be most difficult without knowledge of how and where the reclaimed firm power would be used, it is submitted that the study does give a measure of the benefits to be derived from interconnection and justifies the following conclusion:

> "By suitably interconnecting the three areas to permit free interchange of power, energy, and reserve generating facilities, by utilizing the Shawinigan facilities to store offpeak energy from neighboring areas, and by installing possibly 125,000 kw additional generating capacity on the St. Maurice River to permit returning the stored energy as firm power during the winter months, it is found possible to convert almost all of the surplus energy of the combined system into firm power.
>
> "Thus a total of 260,000 kw of additional firm power may be gained (Table II, item 5), based on the combined system firm load factor under normal conditions of 74.6 per cent. This is illustrated by the curves Figure 3E. The capital expenditure involved for additional generating capacity and suitable tie-line connections has been estimated to be approximately $38 per kilowatt of firm power reclaimed, as against $100 to $125 per kilowatt for new power developments."

Apart from the low cost and more efficient use of resources, it was realized that proper interconnection would be expected to result in better operating flexibility, increased service security, and a number of miscellaneous advantages, such as:

The actual situation in this respect in 1939 was:

(*a*). More complete utilization of surplus energy and less combined reserve generating capacity required.

(*b*). Better regulation of load, frequency, and voltage.

(*c*). Full advantage taken of diversity in load and hydraulic conditions.

(*d*). Better maintenance schedules of equipment possible.

(*e*). Deferment of individual system additions to generating plants.

V. Prewar Conditions

Although from many such studies and frequent discussions, the advantages of adequate system interconnection were well known, there was no economic justification for proceeding, during the prewar period, with the execution of any plan until the reclaimed power could be sold as firm power.

(*a*). Each system had large amounts of surplus power and energy available and was able to derive some revenue from the sale of such power to electric boilers, while firm load growth was proceeding normally.

(*b*). Since the Montreal and Shawinigan areas were interconnected in a limited manner, a portion of the surplus energy in Montreal area was utilized for electric-boiler load on the Shawinigan system.

(*c*). The Shawinigan and Saguenay areas were not interconnected, but some 30,000 kw to 50,000 kw were being interchanged, when desirable, by transferring blocks of load from one area to the other on the 60-kv system.

(*d*). Companies had to abide by commitments and contractual obligations in regard to new plant construction, and, therefore, there was no incentive to defer construction programs and to obtain prime power from neighboring companies by interconnection.

VI. Changed Conditions Caused by the War

With the advent of the war, there was an urgent and immediate demand for large amounts of additional firm power for defense industries. This abnormal demand was not confined to any one area but was most pronounced in the Saguenay and Shawinigan areas. As a result, some form of co-ordination involving proper system interconnection was immediately sought because of the attractive prospect of providing additional firm power in the shortest time at minimum cost.

Compared to the studies previously made on a prewar basis, war conditions modified the situation to some extent, and the actual trend for full development and utilization of total power resources, was along the following lines:

1. In the *Montreal area* there were immediately available some 825,000,000 kilowatt-hours of surplus energy annually which could be converted into prime power by utilizing Shawinigan storage. Such energy was available in the largest amounts during the summer months and during nights and week-

To utilize this energy necessitated increased back-feed from the 110-kv Montreal system and resulted in the installation (January 1942) of a 110-kv oil-filled underground cable seven miles long.[1] This eliminated the restriction or "bottleneck" in power flow through the 60-kv ring system and through the two 30,000-kva transformer banks in the city of Montreal and increased the transfer ability by over 100,000 kw.

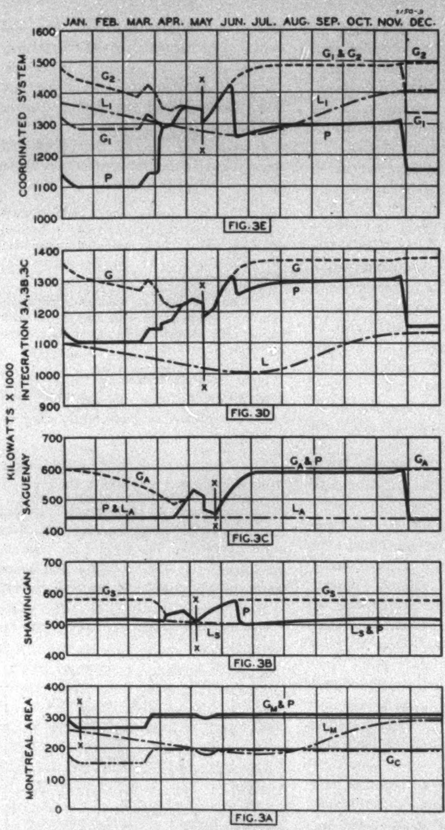


Figure 3. Curves to illustrate graphically the results of co-ordination study based on actual prewar system data

P=available firm power on a *daily* basis of at least normal system load factor. The variable shape of the curve is caused by water shortage or high tail-water conditions

G_M, G_S, G_A= maximum possible generator output at any instant on the individual Montreal, Shawinigan, and Saguenay systems respectively, as influenced by variable heads and flow conditions

G_C=actual Montreal area generator capacity, 1939, apart from Shawinigan contract

G=integration of G_M, G_S, and G_A, $G_2 = G + 125{,}000$-kw maximum system generator output with energy backing on a daily basis

$G_1 = G_2 - (G_A - P)$ =maximum system generator output with energy backing (normal-system firm load factor) from *seasonal* storage

L_M, L_S, L_A=maximum practical firm-power demand (at normal load factor) on the Montreal, Shawinigan, and Saguenay systems, respectively, and as dictated by maximum generator output at the critical point $X-X$ or shortage of water

These curves follow the typical or normal annual trend on the system concerned

L=integration of L_M, L_S, L_A

$L_1 = L + 260{,}000$ kw=estimated firm peak demand (at normal- system load factor) which could be successfully carried on the fully co-ordinated system

$X-X$=Critical points of generator reserve [since the Montreal Light, Heat, and Power Consolidated contract supply from Shawinigan Water and Power Company is duplicated in the output figures of each company (Figures 3A and 3B) a correction in the form of a deduction of 120,000 kw, (160,000 horsepower) has been made in the integrated curves (Figures 3D and 3E) to obtain the true result]

2. In the *Shawinigan area* it was necessary to provide means for handling the increased feed-back from the Montreal area, as well as to store a portion of it, and transmit the remainder to the Saguenay area. This required certain changes on the system and included these items:

(*a*). Additional transformers and voltage regulating equipment were installed in certain districts to permit interchanging the full amount of power and to enable the reactive to be controlled.

(*b*). A number of additional storage projects were started on the St. Maurice River which, when completed, will provide additional capacity of 285 square-mile-feet equivalent to 16,000 horsepower years.

(*c*). To transmit the reclaimed firm power to the Saguenay area, a 220-kv transmission line was built and placed in operation December 1940. This line serves as a means of permanently interconnecting the Saguenay system with the Shawinigan and Montreal systems.

3. The *Saguenay Companies* decided on immediate industrial expansion and complete development and local utilization of power resources in the Saguenay area, as well as arranging for absorbing all available surplus power in the Shawinigan and Montreal areas. Physically, the program included:

(*a*). The construction of two storage reservoirs which, when completed, will provide the means of converting surplus summer energy in the Saguenay area into firm power. Storage capacities are estimated to be 2,460 and 5,360 square-mile-feet respectively and should permit an increase in the regulated flow of the Saguenay River from 32,000 to 45,000 cubic feet per second.

Table II. Study of Kilowatt-Hour Resources and Load Requirements

Based on Normal Prewar Conditions 1939

Item No.	Maximum Feasible Firm Loading If Operated as Isolated Systems: Annual Peak Demand, Kw	Annual Load Factor (Per Cent)	Equivalent Firm Energy Kwhr×10⁶	Total Practical Energy Available Annually Kwhr×10⁶	Surplus Energy Available Annually Kwhr×10⁶
1. Montreal area	293,000	47.5	1,220	2,045	825
2. Shawinigan area	520,000	71.0	3,240	3,600	360
3. Saguenay area	440,000	93.0	3,580	4,100	520
Total	1,253,000	73.0	8,040	9,745	1,705
* Less Montreal Light, Heat, and Power Consolidated contract	120,000		620	620	
4. Integration of items 1, 2, and 3. Net =	1,133,000	74.6	7,420	9,125	1,705

5. Combined surplus energy, if properly utilized and if converted into firm power, would be equivalent to:

$$\frac{1{,}705{,}000{,}000}{8{,}760 \times 0.746} = 260{,}000 \text{ kw}$$

6. Percentage of total energy resources reclaimable as firm power $= \frac{1{,}705}{9{,}125} = 18.6$ per cent

* Since Montreal Light, Heat, and Power Consolidated contract supply from Shawinigan Water and Power Company is duplicated in the output of each company, its deduction is necessary to obtain a true result.

(*b*). Completion of a second double-circuit 154-kv line.

The result has been that with the provision of these facilities a rapid and fairly complete exploitation of all available power and energy resources in these areas has been accomplished with minimum outlay and in minimum time, providing as well additional generator reserve, improved operating flexibility, and service security.

As an illustration of the actual results obtained in combined system output, a typical daily firm-power peak load curve (Figure 4) for January 1942 has been plotted and shows how fully the resources are being utilized for firm-power purposes. It is to be noted that on the individual systems the margin of generator reserve is very limited, whereas on the combined system fairly large reserves are available excepting at time of maximum system peak.

VII. Analyzer Studies and Increased Fault Currents

Anticipating the tying up of the Saguenay and Shawinigan systems over the 220-kv tie line, a number of studies were made on an a-c network analyzer to determine among other matters:

(*a*). The magnitude of fault current at different locations.

(*b*). Possibilities of automatic reclosing and single-phase switching on the 154-kv and 220-kv lines.

(*c*). Degree of stability.

These studies indicated that the interconnection materially increased the magnitude of the fault currents at the interconnecting points.

In most cases, existing oil circuit breakers were capable of meeting the increased duty, but in certain localities it was found necessary to modify existing breaker designs in order to increase the rupture duty and to reduce operating times.

Where new circuit breakers were found to be necessary, there was a tendency to adopt the air-blast breaker for 69-kv, 132-kv, and 220-kv service. The type of air-blast breaker generally adopted was developed by engineers of the Montreal Light, Heat, and Power, Consolidated after several years of development work and field tests. However, also on the system may be found European designs of 11-kv, 154-kv, and 220-kv air-blast circuit breakers, as well as 69-kv and 138-kv "oil-poor" breakers. Circuit-breaker performance is considered satisfactory, although in some instances it has been necessary to modify designs slightly.

VIII. Stability and High-Voltage Automatic Reclosing

In the matter of automatic reclosing of high-voltage lines, actual experience gained over a period of eight years with an installation on two long 187-kv lines indicates that under many transient faults returning the faulted circuit to service promptly by automatic means has prevented the loss of the second line due to the systems falling out of synchronism.

Studies on the analyzer of the Shawinigan-Saguenay 220-kv tie line indicated that with a line-to-ground fault on the line cleared in five cycles, using conventional three-phase switching, and reclosing automatically in 20 cycles, the resulting swing curves showed every indication that stability would be maintained. Since the subsequent line loading is much higher than that used in the studies, attention is now being directed to complete a study of single-phase switching. In due course it is expected that some definite progress will be made in a practical application.

On the two 187-kv circuits, now operating at 154 kv, a few cases of instability have been experienced caused primarily by line short circuits in the Saguenay area and under unusually heavy load conditions. Such operations were not unexpected, and, considering the transmission constants and excessive loading, completely stable operation under all types of faults is not anticipated.

Continual efforts are being made to improve system protection so as to reduce time of fault clearance, with the object of improving stability.

IX. 220-Kv/165-Kv Transformation

In order to connect the 220-kv Shawinigan system to the Saguenay system (operating at 154 kv to 165 kv) and to permit a load of 150,000 kw to be carried it was necessary to consider carefully the transformation problem.

Originally, two obvious ideas were considered:

(*a*). The installation of three 50,000-kva conventional double-winding 220-kv/165-kv three-phase transformers with a limited-capacity tertiary winding.

(*b*). The installation of two 75,000-kva 220-kv/165-kv three-phase autotransformers.

In the case of the conventional-type transformers, it was found that 50,000

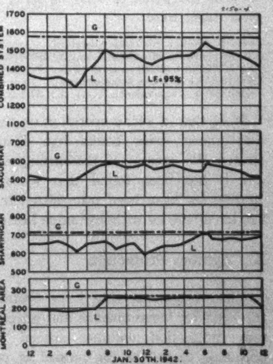


Figure 4. Actual daily load curve, interconnected systems, January 30, 1942, showing how available resources have been fully utilized for firm power purposes

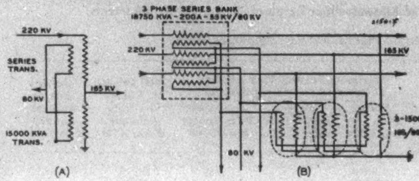


Figure 5. Diagram to illustrate method of interconnecting 220-kv system to 165-kv and 60-kv systems using series transformers

A. Development of one phase

B. 220-kv/165-kv/60-kv transformation

kva was the largest size that could be handled from the point of view of transportation and servicing, and this would have required the purchase of three such units.

Regarding the use of autotransformers, because of their inherent lighter weight, it was possible to obtain from the manufacturer units of 75,000-kva rating so that only two transformers were required.

However, a third proposal was suggested, diagram of which is shown in Figure 5. This scheme was found to be practical by the manufacturer and was eventually used. The fundamental idea was to make use of the fact that there were already existing two 45,000-kva banks of 165-kv star to 66-kv delta. Since the problem was to transmit power from the 220-kv system to the 165-kv system, two three-phase "series" transformers were installed, each having a name-plate rating of 18,750 kva, 220 kv–55 kv/60 kv 196.8*A*, and supplied with a number of suitable voltage taps. The setup is really a scheme whereby use is made of the core structure of the existing 45,000-kva transformers in conjunction with the "series" transformers by electrically coupling at 60 kv, so as to simulate the effect of an autotransformer in the combined unit. By this means, the weight and rating of the additional transformer equipment may be considerably reduced without impairing the kilovolt-ampere transfer ability between 220 kv and 165 kv. Furthermore, the use of this idea results in a marked decrease in impedance and transformation losses between 220-kv, 165-kv, and 60-kv systems, as compared with the alternative schemes.

From the point of view of costs the percentages were:

Three 50,000-kva conventional-type three-phase transformers..........	100 per cent
Two 75,000-kva autotransformers..	70 per cent
Two 18,750-kva series transformers (150,000 kva equivalent)............	57 per cent

More than a year's operation on these three-phase series banks has demonstrated the success of the arrangement and it has been found that loads of 190,000 kw have been transmitted from 220 kv to 165 kv without undue heating.

X. System Protection

When interconnecting large systems, the need for instantaneous and selective zone protection assumes special importance, to reduce the hazard of permanent power arc damage and system instability, since in many instances troubles pyramid.

Fortunately, on this high-voltage network and particularly on the Shawinigan system, the matter of such relay protection had been given unusual attention. The double step impedance principle, with its features of instantaneous fault clearance to within 80 per cent to 90 per cent of the line length, was conceived, developed, and applied as early as 1920 for the clearance of phase-to-phase and ground faults. General application of the instantaneous differential-current zone protection, to generators, transformers, and station bus bars quickly followed.

With the adoption of high-voltage network distribution came the problem of instantaneous protection for the short tie lines. This led to the development and successful application of a number of differential-current pilot-wire schemes, using a separate control cable for the pilot wires. Subsequently, the directional impedance interlock principle was developed, with available communication circuits being used as a control channel.

Recently commercial carrier-current relaying, combined with a simplex communication, has been used in conjunction with some important high-voltage trunk lines to provide selective instantaneous clearance.

For transformer and regulator protection, in addition to differential schemes and supplemented by residual current features, gas-detector relays are being used. The pressure element is arranged to trip the associated circuit breakers, while the gas element operates an alarm.

For standby or backup protection, the impedance principle has been adopted almost exclusively since its initial development because of its effectiveness under all operating conditions and irrespective of the amount of system generator capacity connected. This is in contrast to the shortcomings, in these respects, of the inverse definite time overload protection previously used.

At controlling points, automatic oscillographs and high-speed recording voltmeters are utilized, as well as transmission-line fault locators, to ensure sufficient available data so that each case of trouble may be fully investigated and proper remedial measures applied.

To ensure proper over-all functioning of the protective schemes, the practice of making "primary" checks and tests on new protection installations is standard.

Concluding this section, it is apparent that without the progress and development just outlined in regard to protective schemes, interconnection of systems would have been a hazardous venture with added service difficulties, rather than the increased service reliability actually obtained.

XI. Basic System-Operating Principles

The general principles underlying the operation of the combined systems may be summed up as follows:

(*a*). To assure satisfactory parallel operation, each company undertakes as far as possible and with a practical spirit of co-operation to maintain load, voltage, and frequency conditions which are most suitable to the common interest, keeping in mind, of course, local problems and obligations to customers.

(*b*). Firm power contracts existing prior to interconnection remain in full force and effect.

(*c*). Operating agreements have been made to cover the supply or purchase of surplus energy.

(*d*). It is the objective of all companies concerned to ensure maximum utilization of all available power resources to assist in the war effort. Equitable energy rates have been agreed upon to make this result possible.

So far, any unusual operating matters which have arisen and which were not covered by agreement, have been promptly and satisfactorily rectified by the operating managements of the respective companies.

XII. Control of Voltage and Reactive

Constant voltage is required at the load in the Saguenay and Montreal areas and on the 60-kv busses. In addition, however, when the following items have been considered, it will be realized that a definite reactive control problem exists:

(*a*). Between the Montreal area and the Shawinigan area, involving the four 110-kv circuits, the load may have to vary between 120,000 kw *delivered to* Montreal and 130 kw *fed back from* Montreal.

(*b*). The load transmitted over the upper St. Maurice 220-kv line varies from 100,000 kw to 250,000 kw and is delivered at the receiving point at approximately 100 per cent power factor, so that most of the reactive kilovolt-amperes of the two generating plants feeding this line is used in transmission and is not available therefore for the load.

(*c*). The load transmitted from Quebec to Lake St. John in the Saguenay area may vary between 25,000 kw and 150,000 kw, and at Lake St. John it is frequently necessary to supply 50,000 kva; that is, the power is received at a leading power factor.

(*d*). System load reductions of the order of 45 per cent occur each Sunday when mills shut down.

(*e*). Only one synchronous condenser (30,000-kva capacity) is available on the system installed at Quebec.

This whole matter of voltage and reactive control was given attention in the a-c network-analyzer studies, and as a result, it was decided to install "on-load" tap-changing gear on all transformers in the Shawinigan area where a tie exists between the 220-kv system and the 110-kv Montreal system, as well as on a customer's 110-kv bank.

So far, with some extra supervision required, use of these facilities has enabled voltage levels to be maintained by proper generator field manipulation, as well as by occasional use of vented hydroelectric units as synchronous condensers, and by changing taps at controlling points when dictated by marked changes in loading conditions.

XIII. Frequency Regulation, Load Control, and Reserve Generation

Normally, generating stations in the Montreal area are operated on base load, with frequency controlled by either Shawinigan or Saguenay systems. Automatic frequency control is available and is used on two generating stations in the Shawinigan area. With manual control no difficulty has been experienced in holding frequency to plus or minus 0.1 cycle and synchronous time to a maximum error of ten seconds. This may not be as close regulation as many other systems obtain but is found to be quite satisfactory in this case. Manual control of frequency has been made more convenient to the operator by bringing the governor motor control to miniature keys on the operator's desk.

Automatic tie-line load control is not used and is not considered essential at the present time, although it would be a definite operating convenience. The long distances between exchange points and generating stations make the application of automatic tie-line control both difficult and expensive.

At the generating station on frequency control, it has been the custom in the past to try to maintain a minimum amount of spare equivalent to a 30,000-kw unit during the peak period, and to retain in service at other generating stations a sufficient number of units to carry the allotted load efficiently, consistent with flow and head conditions, so that when the need arose, an additional 30,000 kw–50,000 kw could be picked up by running wide open temporarily. However, present practice is to utilize, as far as possible, most of the normal generator reserve for supplying urgent defense loads. This results occasionally in the necessity of dropping blocks of electric furnace load or grinder motors as a temporary means of maintaining suitable frequency.

XIV. Summary and Conclusions

1. The interconnected system described and supplied solely from hydroelectric plants must be considered as one of the major power systems on the continent, having resources of nearly 4,000,000 horsepower (developed or under construction), of which 2,586,000 horsepower are now operating in parallel.

2. It is to be noted that while operating as individual systems or areas, the hydraulic power resources and load requirements of each group were not ideally matched, it was found that when viewed as a combined system, these characteristics were quite complementary, thus enabling much more complete firm-power utilization of total energy resources.

3. Under normal prewar conditions, undoubtedly this interconnection of systems would have been delayed in its execution, but because of the immediate demand for more firm power for defense industries, it became imperative and economically feasible to proceed, therefore in effect making available for firm power over 18 per cent of the total combined energy resources which otherwise would have been wasted or sold as secondary power to electric boilers. This estimated gain in firm power output is apart from that obtained from additional sources as given in section VI.

4. Because of the necessity of producing and utilizing every available kilowatt-hours for defense purposes, operating procedure has changed, resulting in larger ranges in frequency and voltage regulation and less generator reserve.

Control of reactive kilovolt-amperes is successfully accomplished mainly by generator field adjustment and use of transformer tap-changing equipment, with the assistance of a single 30,000-kva synchronous condenser.

Reference

1. **Design, Manufacture, and Installation of 120-Kv Oil-Filled Cables in Canada, D. M. Farnham, O. W. Titus.** AIEE Transactions, volume 61, 1942, December section, pages 881–8.

Three-Winding Transformer Ring-Bus Characteristics

G. W. BILLS
ASSOCIATE AIEE

C. A. MacARTHUR
NONMEMBER AIEE

SUCH hydroelectric projects as the Boulder Dam, Grand Coulee, and that proposed for the St. Lawrence River present serious power limit and short-circuit problems. Each of these developments will be capable of producing over 1,000,000 kw, part of which for each case must be transmitted over 200 miles. Any method which will increase transient stability and lower fault currents should be carefully investigated; this paper presents the investigation of such a method.

Statement of Problem

To obtain optimum use of transmission lines between a generating station and a load center, it is desirable to parallel the circuits in such a manner that the loss of a line or section of a line will result in the least disturbance. In general, this is done by bussing the lines together and sectionalizing at one or more points.

However, to bus lines on the sending and receiving ends imposes very severe requirements on switching equipment. For example, the 287.5-kv Boulder Dam–Los Angeles lines require 287.5-kv breakers with interrupting capacities of 2,500,000 and 3,500,000 kva. Even on 230-kv systems circuit breakers with interrupting capacities of 2,500,000 kva are in use.

High-voltage bus reactors may be used to reduce short-circuit currents, but besides being costly, they tend materially to increase the reactance between the sending and receiving ends of the system after the loss of line or line section. It is probable, even though the fault currents on the circuits are reduced by the addition of reactors to the bus, that the dynamic power limit would be decreased because of the added impedance.

The use of reactors in the transformer neutrals to reduce ground-fault currents may interfere with relaying and imposes higher-voltage stresses on equipment. Also, for three-phase faults neutral reactors have no effect on fault currents and thus do not increase the power limit for such disturbances.

Description of Studies

Three-winding transformers connected so as to form a ring on the low-voltage side, as shown in Figure 1, lower short-circuit currents by making use of the reactance between low-voltage windings. To determine the transient power limits and fault currents of a system using this ring-bus arrangement, studies were made on an a-c network analyzer. For comparison similar studies were made on the same system with a standard high-voltage bus, as shown in Figure 2.

The studies prove that the transient stability limits are higher and the fault currents are lower for the three-winding transformer ring-bus system. A very interesting result was that the transient stability limits for the above system are almost independent of the reactance between low-voltage windings as it varies from zero to 40 per cent.

The basic system for the studies consisted of four 230-kv transmission lines 185 miles in length connecting a hydroelectric plant to a load center. The load center was connected through a short line to an infinite bus. The generating plant consisted of eight identical 60-cycle machines, each of 55,000-kva capacity. Each generator had a direct-axis transient reactance of 44 per cent and a WR^2 of 132,500,000 pound-feet squared.

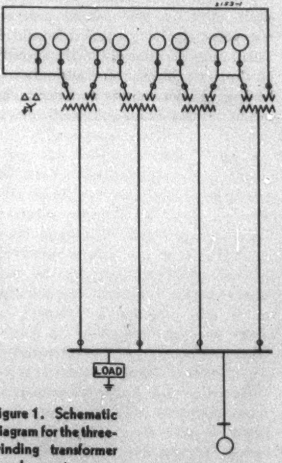


Figure 1. Schematic diagram for the three-winding transformer bus system

For the three-winding transformer ring bus the transformer banks were rated 110,000/110,000/150,000 kva at 13.8/13.8/230 kv and connected delta, delta, wye-grounded. With the low-voltage windings paralleled, the through reactance of the transformers was 11 per cent on a 150,000-kva base.

For the standard bussing arrangement the transformer banks were rated at 110,000 kva, 13.8/230 kv and connected delta, wye-grounded. The reactance was 11 per cent on a 110,000-kva base. Transformer banks rated at 110,000 kva but having a reactance of 11 per cent on a 150,000-kva base were also used.

The various sytems were placed on an a-c network analyzer and studies made of short-circuit and transient-stability characteristics. Faults were applied on a generator bus and also on the sending end of a transmission line. For the three-winding transformer ring bus the reactance between the low-voltage windings was varied from zero to 40 per cent.

The results of the short-circuit studies, shown in Figure 3, indicate that the severity of three-phase short circuits on the generator bus of the transformer ring-bus system can be made as low as that of the standard bus system. This is accomplished by using low-voltage windings having a reactance between them of at least 42 per cent.

By not bussing the transformers on the high-voltage side, the severity of faults on the transmission lines near the generating station are very greatly reduced. This materially reduces system disturbances resulting from the line faults.

Transient-stability limits for the two standard bus systems were found for both three-phase and two-phase faults on the lines near the sending end. For these and all stability studies a total clearing time of $4^1/_2$ cycles was assumed.

Similar studies were made on the three-winding transformer bus system as the reactance between the low-voltage windings was varied from zero to 40 per cent.

The power-limit values from Figure 4 show that the standard systems are much less stable in all cases. For example, the best standard system carries at least 36,000 kw less than can the transformer ring-bus system.

Paper 42-153, recommended by the AIEE committee on power transmission and distribution for presentation at the AIEE Pacific Coast convention, Vancouver, British Columbia, Canada, September 9–11, 1942. Manuscript submitted June 1, 1942; made available for printing July 24, 1942.

G. W. BILLS and C. A. MACARTHUR are both with the Bonneville Power Administration, Portland, Oreg., as assistant engineers in the system engineering department.

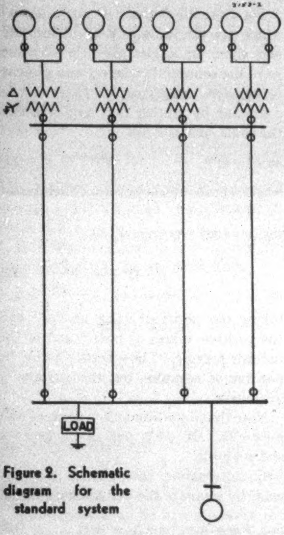


Figure 2. Schematic diagram for the standard system

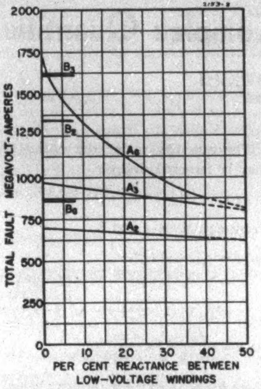


Figure 3. Short-circuit characteristics

Curves *A* for the three-winding transformer ring-bus system as follows:

A_G—Three-phase fault on a generator bus

A_3—Three-phase fault on transmission line near sending end

A_2—Double-line-to-ground fault on a transmission line near sending end

Curves *B* for the standard bus system with 11 per cent transformers on a 110,000-kva base as follows:

B_G—Three-phase fault on a generator bus

B_3—Three-phase fault on a transmission line near sending end

B_2—Double-line-to-ground fault on a transmission line near sending end

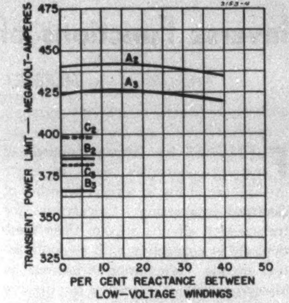


Figure 4. Transient-stability limits

Curves *A* for the three-winding transformer ring-bus system as follows:

A_3—Three-phase fault on transmission line near sending end

A_2—Double-line-to-ground fault on transmission line near sending end

Curves *B* for the standard system with 11 per cent transformers on a 110,000-kva base as follows:

B_3—Three-phase fault on transmission line near sending end

B_2—Double-line-to-ground fault on a transmission line near sending end

Curves C for the standard system with 11 per cent transformers on a 150,000-kva base

C_3—Three-phase fault on a transmission line near sending end

C_2—Double-line-to-ground fault on a transmission line near sending end

It should be kept in mind that the assumed clearing time is almost the minimum possible for 230-kv systems. Longer clearing times would increase the difference in stability limits between the systems and so favor the ring-bus system.

The rise in power limits for the ring-bus system as the transformer reactance increases from zero to 15 per cent is explained by the decreasing severity of the faults. At 15 per cent reactance the effect of the bus reactance in decreasing synchronizing power between the generators becomes larger than that due to decreased fault currents, and the power limits tend to drop when the reactance is increased. However, the variation in power limit is small, being less than two per cent over the entire range; thus higher reactances between low-voltage windings may be used, resulting in greatly reduced currents for generator bus faults.

Conclusions

While a detailed economic study is beyond the scope of this paper, it may be mentioned that three-winding transformers with the ranges of reactance used in this study may be obtained for the same price as standard three-winding transformers. The increase in cost of the three-winding transformers over the smaller capacity two-winding transformers is partially offset by the elimination of high-voltage breakers.

However, on transient stability and short-circuit considerations the three-winding transformer ring bus offers very definite advantages for systems of large magnitudes.

Inverse Functions of Complex Quantities

HERBERT B. DWIGHT
FELLOW AIEE

FORMULAS for inverse functions of complex quantities, such as $\sin^{-1}(x+iy)$, are of use in several branches of electrical engineering. Calculations for transmission circuits require them, particularly in connection with communication circuits. Integration of expressions involving complex quantities can involve inverse functions. They are encountered also in conformal transformations.

In presenting the formulas in this paper, it is necessary to specify angles with sufficient detail to avoid ambiguity and the liability of incorrect results. Also, all of the appropriate multiple values should be included. It is not always obvious by inspection that two different complex values are only values of different branches of the same function and are both correct.

In all cases, numerical examples are given as illustrations.

Inverse Sine $\sin^{-1}(x \pm iy)$

Let

$$\sin^{-1}(x+iy) = u+iv$$

where x, y, u, and v are real quantities and $i=\sqrt{-1}$.

$$\sin(u+iv) = (x+iy)$$

$$\sin u \cosh v = x \tag{1}$$

$$\cos u \sinh v = y \tag{2}$$

Squaring equations 1 and 2 and putting

$$\sin^2 u = 1 - \cos^2 u$$

and

$$\cosh^2 v = 1 + \sinh^2 v$$

there is obtained, by eliminating $\sinh^2 v$ $\left(\text{put } \sinh^2 v = \dfrac{y^2}{\cos^2 u}\right)$

$$\cos^4 u - (1-x^2-y^2)\cos^2 u - y^2 = 0 \tag{3}$$

from which

$$\cos^2 u = {}^1/_2[1-x^2-y^2 \pm \sqrt{(1-x^2-y^2)^2+4y^2}] \tag{4}$$

Since u is real and $\cos^2 u$ is positive, the positive value of the root is taken.

The quantity under the radical sign may be factored, and is

$$\{(1+x)^2+y^2\}\{(1-x)^2+y^2\} \tag{5}$$

From equations 4 and 5

$$\sin^2 u = {}^1/_2[1+x^2+y^2 - \sqrt{\{(1+x)^2+y^2\}\{(1-x)^2+y^2\}}]$$

$$= {}^1/_4[\sqrt{(1+x)^2+y^2} - \sqrt{(1-x)^2+y^2}]^2$$

$$\sin u = \pm\left(\frac{p-q}{2}\right) = \frac{\pm 2x}{p+q} \tag{6}$$

$$\text{where } p = \sqrt{(1+x)^2+y^2} \text{ (positive value)} \tag{7}$$

$$q = \sqrt{(1-x)^2+y^2} \text{ (positive value)} \tag{8}$$

The second solution in equation 6 is obtained by rationalizing the numerator. It involves the sum of two quantities instead of the difference and so allows more convenient precise computation.

Since v is real, $\cosh v$ is positive, as may be seen from the series expansion. Therefore, by equation 1, $\sin u$ is the same sign as x. This allows ± in equation 6 to be changed to +. Therefore

$$u = \sin^{-1}\frac{2x}{p+q} \tag{9}$$

Take the principal value of u, that is, the value between $-\pi/2$ and $\pi/2$. Since u lies in this range, $\cos u$ is positive.

The following expression corresponding to equation 3 can be obtained by eliminating $\cos^2 u$

$$\sinh^4 v + (1-x^2-y^2)\sinh^2 v - y^2 = 0$$

$$\sinh^2 v = {}^1/_2[x^2+y^2-1 \pm \sqrt{(1-x^2-y^2)^2+4y^2}]$$

The quantity under the radical sign is the same as equation 5 so that

$$\cosh v = \pm {}^1/_2(p+q)$$

Since v is real, the positive value is to be taken.

$$v = \cosh^{-1}\frac{p+q}{2} \tag{10}$$

Since $\cos u$ is positive, then by equation 2, $\sinh v$ and v are the same sign as y. This may be secured by writing

$$\sin^{-1}(x \pm iy) = \sin^{-1}\frac{2x}{p+q} \pm i\cosh^{-1}\frac{p+q}{2}$$

where y is positive and the positive value of $\cosh^{-1}$ is taken (see reference 1, page 58, and reference 2, page 264).

For any angle θ, there are angles $2k\pi+\theta$ which are the same as θ in all respects, and there are angles $(2k+1)\pi-\theta$ which have the same sine, where k is a positive or negative integer or 0. These can be combined by stating that angles having the same sine as θ are

$$n\pi + (-1)^n\theta \tag{11}$$

where n is an integer or zero. Therefore

$$\sin^{-1}(x \pm iy) = n\pi + (-1)^n \sin^{-1}\frac{2x}{p+q} \pm i(-1)^n\cosh^{-1}\frac{p+q}{2} \tag{12}$$

taking the principal value of $\sin^{-1}$ and the positive values of $\cosh^{-1}$ and of the radicals p and q. The quantity x may be positive or negative, but the quantity y is positive.

Note that if $y=0$ and $x>1$, $q=x-1$ and $p+q=2x$. If $y=0$ and $x<1$, $q=1-x$ and $p+q=2$.

An alternative computation may be made by means of the well-known formula

$$\sinh^{-1} m = \operatorname{logn}(m+\sqrt{m^2+1}) \tag{13}$$

where logn denotes natural logarithm. Let

$$\sin^{-1} A = u$$

where A and u are complex quantities

$$\sin u = A$$
$$iA = \sinh iu$$
$$iu = \sinh^{-1} iA$$
$$\sin^{-1} A = -i\sinh^{-1} iA + 2k\pi$$

$$= -i\operatorname{logn}(\pm\sqrt{1-A^2}+iA)+2k\pi \tag{14a}$$

$$\text{or } = i\operatorname{logn}(\pm\sqrt{1-A^2}-iA)+2k\pi \tag{14b}$$

The two solutions of equation 14a indicated by ± correspond to the two angles θ and $\pi-\theta$ which have the same sine. The second or alternative form, equation 14b, is identical with equation 14a. The expression involving + in equation 14b is obtained from that involving + in equation 14a by rationalizing the numerator in equation 14a. In practice, the form should be used which involves the numerical sum of quantities instead of the difference, thus giving more convenient precise computation.

Square Root $\sqrt{(x \pm iy)}$

In the computation just described, the square root of a complex quantity is required. It may be expressed as follows:

$$\sqrt{(x+iy)} = \pm\left[\sqrt{\left(\frac{m+x}{2}\right)} + i\sqrt{\left(\frac{m-x}{2}\right)}\right] \tag{15}$$

Paper 42-158, recommended by the AIEE committee on basic sciences for presentation at the AIEE Pacific Coast convention, Vancouver, British Columbia, Canada, September 9–11, 1942. Manuscript submitted April 30, 1942; made available for printing July 27, 1942.

HERBERT B. DWIGHT is professor of electrical machinery, Massachusetts Institute of Technology, Cambridge, Mass.

$$\sqrt{(x-iy)}=\pm\left[\sqrt{\left(\frac{m+x}{2}\right)}-i\sqrt{\left(\frac{m-x}{2}\right)}\right] \tag{16}$$

where x may be positive or negative, y is positive
and

$$m=+\sqrt{(x^2+y^2)} \tag{17}$$

The positive square roots of $(m+x)/2$ and $(m-x)/2$ are used (see reference 2, page 260).

An alternative method is to express the complex quantity in the polar form

$$r\,\angle\theta=re^{i\theta}=r(\cos\theta+i\sin\theta) \tag{18}$$

where, if the complex quantity is $x+iy$

$$r=+\sqrt{(x^2+y^2)},\ \cos\theta=\frac{x}{r}\text{ and }\sin\theta=\frac{y}{r} \tag{19}$$

Then

$$\begin{aligned}\sqrt{(x+iy)}&=\pm\sqrt{r}\,\underline{/\theta/2}\\&=\pm\sqrt{r}\ \cos\frac{\theta}{2}+i\sin\frac{\theta}{2}\Big)\end{aligned} \tag{20}$$

The angle θ may be in any one of the four quadrants, depending on whether x and y are positive or negative quantities. The angle is not specified according to the principal values of $\tan^{-1}$ and $\cos^{-1}$, and so forth, though the numerical value of θ may be conveniently found by using a table of tan or $\tan^{-1}$ and then determining the quadrant for θ by equation 19.

Logarithm logn (x+iy)

In using equation 14, the logarithm of a complex quantity is required. This is computed as an inverse function of e^z.

Let

$$\begin{aligned}&\text{logn}\ (x+iy)=u+iv\\&e^{u+iv}=e^u\ (\cos v+i\sin v)\\&\quad=x+iy\end{aligned}$$

$$e^u\cos v=x \tag{21}$$

$$e^u\sin v=y \tag{22}$$

Squaring and adding

$$e^{2u}=(x^2+y^2)$$

Let

$$r=\sqrt{x^2+y^2}$$

The positive value of the root is to be taken since u is real and e^u is positive.

The angle v is to be specified with sufficient completeness so that the numerical values of cos v and sin v will have the correct signs.

$$\text{logn}\ (x+iy)={}^1\!/_2\ \text{logn}\ (x^2+y^2)+i(\theta+2\pi k) \tag{23}$$

where $\cos\theta=x/r, \sin\theta=y/r, r=\sqrt{(x^2+y^2)}$ and k is an integer or 0, the positive value of r being taken. The quantities x and y may be positive or negative (see reference 7, page 3).

The angle θ, according to this specification, is not always a principal value of $\cos^{-1}$, $\sin^{-1}$, or $\tan^{-1}$. If both x and y are negative, tan θ is positive and the angle θ is in the third quadrant.

Another case where

$$\theta=\tan^{-1}\frac{m}{n}$$

is not a sufficient specification is in the equation

$$m\cos A+n\sin A=r\sin(A+\theta) \tag{24}$$

where $r=\sqrt{m^2+n^2}$, $\sin\theta=m/r$ and $\cos\theta=n/r$
(See reference 5, number 401.2).
Example 1. $\sin^{-1}(2+i3)$

$$\begin{aligned}p&=\sqrt{(9+9)}=4.243\\q&=\sqrt{(1+9)}=\underline{3.162}\\p+q&=7.405\end{aligned}$$

$$\sin^{-1}\frac{4}{7.405}=\sin^{-1}0.540=0.570\text{ radian}$$

$$\cosh^{-1}\frac{7.405}{2}=1.983$$

Putting $n=0$ in equation 12,

$$\sin^{-1}(2+i3)=0.570+i1.983$$

Putting $n=1$

$$\begin{aligned}\sin^{-1}(2+i3)&=3.142-0.570-i\ 1.983\\&=2.572-i\ 1.983\end{aligned}$$

In this computation, tables of $\sin^{-1}x$, $\cosh^{-1}x$, and so forth, as in reference 6, for real values of x, may be used. To check,

$$\begin{aligned}\sin(0.570+i\ 1.983)&=0.540\times3.702+\\&\qquad i\ 0.842\times3.563\\&=2.00+i\ 3.00\end{aligned}$$

and

$$\begin{aligned}\sin(2.572-i1.983)&=0.540\times3.702-\\&\qquad i(-0.842\times3.563)\\&=2.00+i\ 3.00\end{aligned}$$

If the only purpose of the real part of the value of $\sin^{-1}(2+i3)$ is to take the sin, cos, or tan, or to add it to other angles which are given in degrees, then a trigonometric table in degrees might be used, but care would be needed in choosing an appropriate notation.

In using equation 14 to obtain $\sin^{-1}(2+i3)=\sin^{-1}A$

$$\begin{aligned}&1-A^2=6-i\ 12\\&\quad=x-iy\qquad\text{as in equation 16}\\&m=\sqrt{(36+144)}=13.42\ \text{by equation 17}\\&m+x=19.42,\ m-x=7.42\\&\sqrt{(1-A^2)}=\sqrt{\left(\frac{19.42}{2}\right)}-i\sqrt{\left(\frac{7.42}{2}\right)}\\&\qquad\text{by equation 16, using the + sign}\\&\qquad=3.116-i\ 1.926\\&-iA=\underline{3\qquad-i\ 2}\\&\sqrt{(1-A^2)}-iA=6.116-i\ 3.926\end{aligned}$$

Equation 14b is used instead of equation 14a so as to avoid the small difference of nearly equal quantities.

Let $6.116-i3.926=x+iy$ as in equation 23

$$\begin{aligned}6.116^2&=37.4\\3.926^2&=\underline{15.4}\\&\ \ 52.8\end{aligned}$$

$${}^1\!/_2\ \text{logn}\ 52.8={}^1\!/_2(1.664+2.303)=1.983$$

The angle θ of equation 23 is in the fourth quadrant.

$$\tan(-\theta)=\frac{3.926}{6.116}=0.642$$

$$\begin{aligned}&\theta=-0.571\text{ radian}\\&\text{logn}\ (x+iy)=1.983-i0.571\\&\qquad\text{by equation 23}\\&\sin^{-1}(2+i3)=0.571+i1.983\\&\qquad\text{by equation 14b}\end{aligned}$$

The second solution is given by the − sign in equation 14a or 14b, the former being preferable.

$$-\sqrt{(1-A^2)}+iA=-6.116+i3.926$$

The angle θ of equation 23 is in the second quadrant.

$$\sin^{-1}(2+i3)=2.571-i1.983$$

The quantity $2k\pi$ may be added to either of these solutions.

Inverse Cosine cos⁻¹(x±iy)

Let $\cos^{-1}(x+iy)=u+iv$

$$\cos(u+iv)\ =x+iy$$

$$\cos u\cosh v\ =x \tag{25}$$

$$\sin u\sinh v\ =-y \tag{26}$$

Squaring and eliminating $\sinh^2 v$,

$$\sin^4 u-(1-x^2-y^2)\sin^2 u-y^2=0$$

$$\sin^2 u={}^1\!/_2[1-x^2-y^2\pm\sqrt{(1-x^2-y^2)^2+4y^2}]$$

Since u is real and $\sin^2 u$ is positive, use the positive value of the root.

$$\begin{aligned}\cos^2 u&={}^1\!/_2(1+x^2+y^2-pq)\\\cos\ u&=\pm{}^1\!/_2(p-q)\end{aligned}$$

Since v is real, cosh v is positive, and cos u is the same sign as x, by equation 25. Then

$$\cos u={}^1\!/_2(p-q)=\frac{2x}{p+q}$$

$$u=\cos^{-1}\frac{2x}{p+q}$$

Take the principal value, that is, the value between 0 and π. Then $\sin u$ is positive. Similarly

$$\sinh^4 v+(1-x^2-y^2)\sinh^2 v-y^2=0$$
$$\sinh^2 v={}^1/_2[x^2+y^2-1+\sqrt{(1-x^2-y^2)^2+4y^2}]$$

taking the positive value of the root since $\sinh^2 v$ is positive.

$$\cosh^2 v={}^1/_4(p+q)^2$$
$$\cosh v={}^1/_2(p+q)$$

taking the positive value since v is real.

$$v=\cosh^{-1}\frac{p+q}{2}$$

Since $\sin u$ is positive, $\sinh v$ and v are the same sign as $-y$, from equation 26.

$$\cos^{-1}(x+iy)=\pm\left[\cos^{-1}\frac{2x}{p+q}+2k\pi-i\cosh^{-1}\frac{p+q}{2}\right] \qquad (27)$$

where y is positive, taking the principal value of $\cos^{-1}$ and the positive values of $\cosh^{-1}$ and of p and q. Also

$$\cos^{-1}(x-iy)=\pm\left[\cos^{-1}\frac{2x}{p+q}+2k\pi+i\cosh^{-1}\frac{p+q}{2}\right] \qquad (28)$$

taking the same values of $\sin^{-1}$ and $\cosh^{-1}$ as with equation 27. For p and q see equations 7 and 8.

The quantity x may be positive or negative. The quantity y is positive.

An alternative method is by use of the equation

$$\cosh^{-1}p=\pm\operatorname{logn}(x+\sqrt{x^2-1})$$

Let $\cos^{-1}A=u$, a complex quantity.

$$A=\cos u=\cosh iu$$
$$iu=\cosh^{-1}A$$

$$\cos^{-1}A=-i\cosh^{-1}A=$$
$$\mp i\operatorname{logn}(A+\sqrt{A^2-1})+2k\pi \qquad (29a)$$

$$\text{or }=\pm i\operatorname{logn}(A-\sqrt{A^2-1})+2k\pi \qquad (29b)$$

The second equation is obtained by rationalizing the numerator of the first and is to be used when it avoids the numerical difference of quantities.

Example 2. $\cos^{-1}(-2-i4)$

By equation 28, putting $k=0$

$$\cos^{-1}(-2-i4)=\pm[\cos^{-1}(-0.4385)+i\cosh^{-1}4.56]$$
$$=\pm[2.025+i2.198]$$

To check,

$$\cos(2.025+i2.198)=-0.4385\times4.56-i0.899\times4.45$$
$$=-2.00-i4.00$$

By the alternative method, let $A=-2-i4$

$$A^2-1=-13+i16$$
$$\sqrt{(A^2-1)}=1.952+i4.10$$
$$A=-2-i4$$

Using equation 29b to avoid the numerical difference of quantities

$$A-\sqrt{(A^2-1)}=-3.952-i8.10$$

In finding the logarithm of this, $\operatorname{logn} r=2.20$, and the angle θ is in the third quadrant and is 4.258 radians.

$$\cos^{-1}(-2-i4)=\pm(-4.258+i2.20)+2k\pi$$
$$=\pm(2.025+i2.20)$$

putting

$$k=1.$$

Inverse Tangent $\tan^{-1}(x+iy)$

Let $\tan^{-1}(x+iy)=u+iv$

$$\tan(u+iv)=x+iy=i\tanh\frac{u+iv}{i}$$

since

$$\tan z=i\tanh(z/i)$$

$$\tanh\frac{u+iv}{i}=\frac{x+iy}{i}$$

$$v-iu=\tanh^{-1}(y-ix)$$
$$={}^1/_2\operatorname{logn}\frac{1+y-ix}{1-y+ix} \qquad (30)$$

where logn denotes natural logarithm.

$$e^{2v}(\cos 2u-i\sin 2u)=\frac{1+y-ix}{1-y+ix}$$

Rationalizing the denominator

$$e^{2v}\cos 2u=\frac{1-x^2-y^2}{(1-y)^2+x^2} \qquad (31)$$

$$e^{2v}\sin 2u=\frac{2x}{(1-y)^2+x^2} \qquad (32)$$

Squaring, adding, and factoring the numerator

$$e^{4v}=\frac{(1+y)^2+x^2}{(1-y)^2+x^2} \qquad \text{by equation 5}$$

$$v=\frac{1}{4}\operatorname{logn}\frac{(1+y)^2+x^2}{(1-y)^2+x^2} \qquad (33)$$

Dividing equation 32 by equation 31

$$\tan 2u=\frac{2x}{1-x^2-y^2} \qquad (34)$$

Let

$$2u=\pi-\tan^{-1}\frac{1+y}{x}-\tan^{-1}\frac{1-y}{x} \qquad (35)$$
$$=\pi-\alpha-\beta$$

where the principal values of $\tan^{-1}$ are taken, that is, α and β are between $-\pi/2$ and $\pi/2$.

$$\sin(\pi-\alpha-\beta)=\sin(\alpha+\beta)$$
$$=(\tan\alpha+\tan\beta)\cos\alpha\cos\beta$$
$$=\frac{2}{x}\cos\alpha\cos\beta$$

which is the same sign as x, as it should be, from equation 32. Cos α and cos β are positive.

$$\cos(\pi-\alpha-\beta)=-\cos(\alpha+\beta)$$
$$=(-1+\tan\alpha\tan\beta)\cos\alpha\cos\beta$$
$$=(1-x^2-y^2)\frac{\cos\alpha\cos\beta}{x^2}$$

which is the same sign as $1-x^2-y^2$ and proportional to it (see equation 31).

The quantity $2k\pi$, where k is an integer, may be added to equation 35.

Therefore

$$\tan^{-1}(x+iy)=\frac{1}{2}\left\{(2k+1)\pi-\tan^{-1}\frac{1+y}{x}-\tan^{-1}\frac{1-y}{x}\right\}+\frac{i}{4}\operatorname{logn}\frac{(1+y)^2+x^2}{(1-y)^2+x^2} \qquad (36)$$

where the principal values of $\tan^{-1}$ are taken and where x and y may be positive or negative.

An alternative method of computation is by means of equation 31

$$\tan^{-1}(x+iy)=\frac{i}{2}\operatorname{logn}\frac{1+y-ix}{1-y+ix}+2k\pi \qquad (37)$$

Example 3

$$\tan^{-1}(-2-i4)=1.675-i0.2006$$

When computing this by equation 37, $k=-1$. Other values, given by adding $2k\pi$, are equally appropriate.

Inverse Hyperbolic Sine $\sinh^{-1}(\pm x+iy)$

Let

$$\sinh^{-1}(x+iy)=u+iv$$
$$x+iy=\sinh(u+iv)$$
$$=i\sin\frac{u+iv}{i}$$
$$y-ix=\sin\frac{u+iv}{i}$$
$$u+iv=i\sin^{-1}(y-ix) \qquad (38)$$

By equation 12

$$\sinh^{-1}(\pm x+iy)=\pm(-1)^n\cosh^{-1}\frac{s+t}{2}+i(-1)^n\sin^{-1}\frac{2y}{s+t}+in\pi \qquad (39)$$

where n is an integer or 0,
x is positive,
y is positive or negative

$$s=\sqrt{(1+y)^2+x^2}\text{ (positive value)} \qquad (40)$$

$$t=\sqrt{(1-y)^2+x^2}\text{ (positive value)} \qquad (41)$$

The principal value of $\sin^{-1}$ (between $-\pi/2$ and $\pi/2$) and the positive value of $\cosh^{-1}$ are taken.

Note that if $x=0$ and $y>1$, $s+t=2y$ and if $y<1$, $s+t=2$.
An alternative solution is

$$\sinh^{-1} A = \operatorname{logn}\left(\pm\sqrt{1+A^2}+A\right)+i2k\pi \tag{42a}$$

$$\text{or} \quad = -\operatorname{logn}\left(\pm\sqrt{1+A^2}-A\right)+i2k\pi \tag{42b}$$

The two solutions of equation 42a indicated by ± correspond to the two angles θ and $\pi-\theta$ which have the same sine. Logn $(A-\sqrt{A^2+1})$ is a value of $\sinh^{-1} A$ which differs from logn $(A+\sqrt{A^2+1})$ by logn (-1) or $i(2k+1)\pi$. Evidently one at least of the two values is complex, for any given value of A, whether A is real or complex.

The second or alternative form, equation 42b, gives the same results as equation 42a. It should be used when it enables one to avoid computing the numerical difference of two quantities.

Example 4

$$\sinh^{-1}(-2-i4) = -2.184-i1.097$$

putting $n=0$ in equation 39 or using the + sign in equation 42b.

Also $\sinh^{-1}(-2-i4)=2.184+i4.239$ putting $n=1$ in equation 39 or using the − sign in equation 42a.

The quantity $i\,2k\pi$ may be added to both these solutions.

The results may be checked by computing sinh, which gives $-2-i4$.

Inverse Hyperbolic Cosine $\cosh^{-1}(x+iy)$

Let $\cosh^{-1}(x+iy)=u+iv$

$$x+iy=\cosh(u+iv)=\cos\frac{u+iv}{i}$$

$$u+iv=i\cos^{-1}(x+iy)$$

If y is positive, by equation 28,

$$\cosh^{-1}(x+iy)= \pm\left[\cosh^{-1}\frac{p+q}{2}+i\cos^{-1}\frac{2x}{p+q}+i2k\pi\right] \tag{43}$$

Also, if y in the following equation is positive, by equation 29,

$$\cosh^{-1}(x-iy)= \pm\left[\cosh^{-1}\frac{p+q}{2}-i\cos^{-1}\frac{2x}{p+q}+i2k\pi\right] \tag{44}$$

The quantities p and q are positive and are given by equations 7 and 8. The quantity x is positive or negative.

The positive value of $\cosh^{-1}$ and the principal value of $\cos^{-1}$ are taken.

An alternative solution is

$$\cosh^{-1} A = \pm\operatorname{logn}\left(A+\sqrt{A^2-1}\right)+i2k\pi \tag{45a}$$

or

$$= \pm\operatorname{logn}\left(A-\sqrt{A^2-1}\right)+i2k\pi \tag{45b}$$

Equations 45a and 45b give the same results and the one which involves the numerical sum of two quantities should be used in any given case.

Inverse Hyperbolic Tangent $\tanh^{-1}(x+iy)$

Let

$$\tanh^{-1}(x+iy)=u+iv = \frac{1}{2}\operatorname{logn}\frac{1+x+iy}{1-x-iy} \tag{46}$$

$$e^{2u}\cos 2v=\frac{1-x^2-y^2}{(1-x)^2+y^2} \tag{47}$$

$$e^{2u}\sin 2v=\frac{2y}{(1-x)^2+y^2} \tag{48}$$

Squaring, adding, and factoring the numerator

$$e^{4u}=\frac{(1+x)^2+y^2}{(1-x)^2+y^2}$$

$$u=\frac{1}{4}\operatorname{logn}\frac{(1+x)^2+y^2}{(1-x)^2+y^2} \tag{49}$$

Dividing equation 33 by equation 32

$$\tan 2v=\frac{2y}{1-x^2-y^2} \tag{50}$$

Let

$$2v=\pi-\tan^{-1}\frac{1+x}{y}-\tan^{-1}\frac{1-x}{y} \tag{51}$$

$$=\pi-\alpha-\beta$$

where the principal values of $\tan^{-1}$ are taken, that is, α and β are between $-\pi/2$ and $\pi/2$.

$$\begin{aligned}\sin(\pi-\alpha-\beta) &= \sin(\alpha+\beta)\\ &= (\tan\alpha+\tan\beta)\cos\alpha\cos\beta\\ &= (2/y)\cos\alpha\cos\beta\end{aligned}$$

which is the same sign as y, as it should be, from equation 48. Cos α and cos β are positive.

$$\begin{aligned}\cos(\pi-\alpha-\beta) &= -\cos(\alpha+\beta)\\ &= (-1+\tan\alpha\tan\beta)\cos\alpha\cos\beta\\ &= (1-x^2-y^2)\frac{\cos\alpha\cos\beta}{y^2}\end{aligned}$$

which is the same sign as $1-x^2-y^2$ and proportional to it (see equation 47). The quantity $2k\pi$, where k is an integer, may be added to equation 51.

Therefore

$$\tanh^{-1}(x+iy)=\frac{1}{4}\operatorname{logn}\frac{(1+x)^2+y^2}{(1-x)^2+y^2}+ \frac{i}{2}\left\{(2k+1)\pi-\tan^{-1}\frac{1+x}{y}-\tan^{-1}\frac{1-x}{y}\right\} \tag{52}$$

where the principal values of $\tan^{-1}$ are taken and where x and y may be positive or negative (reference 3, page 115, and reference 5, number 715).

An alternative method of computation is to use equation 46 directly. Multiple values will be obtained since expression 23 for logn has a term $i2k\pi$.

Example 6

$$\tanh^{-1}(-2+i3)=-0.1469+i1.339$$

putting $k=0$ in equation 52

$$\text{or} = -0.1469+i4.4806$$

putting $k=1$.

The choice of the alternative formulas involving logarithms of complex quantities, which have been given in each case in this paper, depends to some extent on the practice and preference of each individual. While the algebraic logarithmic formulas are shorter to write than the others, the solution of the numerical problems in this paper has seemed longer by the logarithmic formulas.

The use of logarithmic formulas for obtaining results involving inverse functions is given in equations 125, page 71 and 428, page 179 of volume 2 of "Communication Networks," by E. A. Guillemin (reference 4).

References

1. Application of Hyperbolic Functions to Electrical Engineering Problems, A. E. Kennelly. McGraw-Hill Book Company, Inc., New York, N. Y. Edition of 1925.

2. Transmission Circuits for Telephonic Communication, K. S. Johnson. D. Van Nostrand Company, Inc., New York, N. Y. Edition of 1939.

3. Principles of Electric-Power Transmission, L. F. Woodruff. John Wiley and Sons, Inc., New York, N. Y., 1938.

4. Communication Networks, E. A. Guillemin. John Wiley and Sons, Inc., New York, N. Y., 1935.

5. Tables of Integrals and other Mathematical Data, H. B. Dwight. The Macmillan Company, New York, N. Y., 1934.

6. Mathematical Tables, H. B. Dwight. McGraw-Hill Book Company, Inc., New York, N. Y., 1941.

7. A Short Table of Integrals, B. O. Peirce. Ginn and Company, Boston, Mass. Edition of 1929.

A Practical Discussion of Problems in Transformer Differential Protection

P. W. SHILL
MEMBER AIEE

Synopsis: Some conditions which have to be met with in protecting transformers by the usual current-differential method produce special problems. Those involving the protection of interconnected wye or zigzag transformers and those involving Scott-connected transformers are dealt with at some length. There is also a discussion of tolerances in the selection of current transformers and the factors to be considered in the selection of relays for the purpose.

ELECTRIC-power systems are becoming more and more complex insofar as their protection is concerned. Until now there has been little difficulty in obtaining almost anything that one might desire for the usual run of problems, such as the protection of machines, transmission lines, transformers, and so forth. Even systems, which have grown to the point where they have generators, transmission lines, transformers, and other equipment with widely diverse characteristics connected together, have been successfully protected. The war has now changed things, so that today we are having to bring back into service various pieces of apparatus which have been considered obsolete, and which one hoped would remain in that class forever. To connect old apparatus into a well-designed power system and protect them both with equipment not too well-suited to the purpose is a real problem for the engineer responsible for relay protection. Often the circuit is needed in service very quickly, but the delivery dates for relays, current transformers, and so forth, cannot be promised for many months ahead. There is also the question of priorities.

It is quite probable that some old two-phase equipment will have to be rehabilitated and put back into service. For this reason, the question of the protection of Scott-connected transformers by the current-differential method should receive some attention.

While there have been on the market several types of relays and current transformers which, when properly applied, give very satisfactory results, we are now faced with the fact that we must use considerable ingenuity to "get by" with such things as we may have on hand or can procure readily.

A few years ago much time and money were spent on current-differential protections to see that the various component parts of the circuit were built as closely to their theoretical ideals as possible. Current transformers of exact special ratios and with matching characteristics were specially purchased and installed. Current-transformer cable connections were carefully tested so that their resistances might be closely matched. Relays of very special types were considered to be absolute essentials. In fact, there was, generally speaking, considerable superstition in some places about this type of problem.

Today, with our accumulated data based on experience and more accurate knowledge of those conditions which must be satisfied in order to ensure a reliable protection, many of the old ideas have been greatly modified, and we know that we can do things which at one time were considered to be quite hazardous to the continuity of service.

A transformer differential protection is much more likely to operate when it should not do so than not to operate when it should do so. So that, provided the relays are in working order and their current windings are not short-circuited, they will operate if they can get sufficient current at any time. The chief concern with differential relays is to see that they do not respond too readily to transient currents and to be sure that their multiple trip circuits are in an operable state at all times. So sensitive is the usual current-differential protection that a loose connection on one current-transformer wire has been known to operate the relay connected with it.

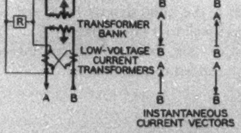

A
B
HIGH-VOLTAGE CURRENT TRANSFORMERS
R
TRANSFORMER BANK
LOW-VOLTAGE CURRENT TRANSFORMERS
A
B
INPUT
OUTPUT
A
B
A
B
A
B
A
B
A
B
A
B
INSTANTANEOUS CURRENT VECTORS

Figure 1. Single-phase transformer differential protection

The chief sources of trouble for which the designer must be on the watch when working up the studies for a differential protection are:

1. The possibility of the generating capacity of the system being so reduced under light load conditions that the fault current available in the protected zone may be insufficient to operate the relays.

2. The possibility that the relays may be so sensitive that they may operate during periods of transient differential currents, which may be produced by the magnetic characteristics of the transformer and of the current transformers, at times of switching, or of heavy faults which draw current through the protected zone but are themselves outside of it.

In the past there was some doubt about the suitability of bushing-type current transformers for use in differential current schemes. It is now accepted that, except in the cases of those with turn ratios too small to be reliable for any kind of use, bushing-type current transformers are as reliable as wound ones for relay work, especially when the relays are required to work, that is, during periods of heavy current flow due to fault conditions.

Fundamental Principles

Let us now review the more important basic conditions which must be satisfied in all reliable current-differential protective schemes for three-phase transformers.

1. Current transformers must be provided in every phase connection to the transformer or transformer bank, so that all current entering and leaving the windings by normal paths may be properly measured and accounted for in the balancing of the differential circuit.

2. Delta-connected transformer windings ordinarily are provided with current transformers with wye-connected secondary windings. They are the most accommodating of all circuits for which to provide differential protection, because, provided proper attention is paid to the ratio of the current transformers, it is immaterial whether their current-transformer secondary circuits be wye-connected or delta-connected.

3. Any transformer windings which are connected in wye or zigzag and are so arranged that current can return to any phase through a neutral connection will at times have some current returning which is not measured by the phase current trans-

---

Paper 42-146, recommended by the AIEE committee on power transmission and distribution for presentation at the AIEE Pacific Coast convention, Vancouver, British Columbia, Canada, September 9–11, 1942. Manuscript submitted May 25, 1942; made available for printing July 2, 1942.

P. W. SHILL is relay protection engineer with the British Columbia Electric Railway Company, Ltd., Vancouver, British Columbia, Canada.

The author expresses his appreciation to L. B. Stacey for his suggestions on the scope and form of the paper, and to J. A. Polson for checking the manuscript and preparing the diagrams.

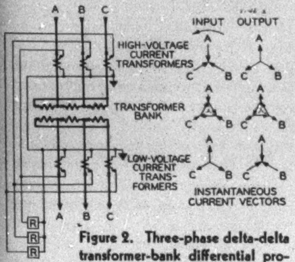

A
B
C
INPUT
OUTPUT
HIGH-VOLTAGE CURRENT TRANSFORMERS
TRANSFORMER BANK
LOW-VOLTAGE CURRENT TRANS-FORMERS
INSTANTANEOUS CURRENT VECTORS
R
R
R

Figure 2. Three-phase delta-delta transformer-bank differential protection

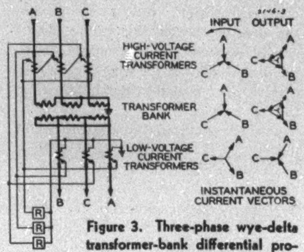

A
B
C
INPUT
OUTPUT
HIGH-VOLTAGE CURRENT TRANSFORMERS
TRANSFORMER BANK
LOW-VOLTAGE CURRENT TRANSFORMERS
INSTANTANEOUS CURRENT VECTORS
R
R
R

Figure 3. Three-phase wye-delta transformer-bank differential protection

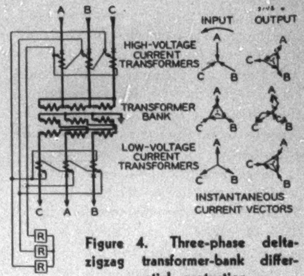

A
B
C
INPUT
OUTPUT
HIGH-VOLTAGE CURRENT TRANSFORMERS
TRANSFORMER BANK
LOW-VOLTAGE CURRENT TRANSFORMERS
INSTANTANEOUS CURRENT VECTORS
R
R
R

Figure 4. Three-phase delta-zigzag transformer-bank differential protection

formers. This would cause an unbalance in the differential circuit. This unbalance will take the form of zero-phase-sequence components in the current-transformer secondary circuits which measure the currents in the wye-connected or zig-zag-connected transformer windings. Currents which do not sum to zero in delta-connected current-transformer secondary circuits will circulate the difference current around the delta. This circulating current is for all practical purposes the sum of the zero-phase-sequence components and corresponds to the power current returning through the neutral connection. The currents leaving from the delta and entering into the differential circuit will be a true measure of the phase power currents and will balance out satisfactorily. Because of these facts, it is generally accepted that circuits connected to wye- or zigzag connected transformer windings *must* have a delta circulating path for the zero-phase-sequence components in their current-transformer secondary circuits. This is necessary to ensure that neutral currents will not upset the differential current balance and thus operate the relays for some external condition when there is no fault in the differentially protected transformer zone.

4. The instantaneous vector sum of all the currents from the current-transformer leads connected to the relay for any one phase should be as nearly as possible zero at all instants of time when no internal fault exists. For this reason the phase angles between all the currents must be theoretically either zero or 180 degrees, depending upon the relation of the power transformer windings to each other, when no internal fault exists.

Table I

Full Load of Transformer Bank = 15,000 Kva 3-Phase
Current-Transformer Ratios Figured for 25 Per Cent Overload. Base 60 Kv

		Current-Transformer Ratios			
Kilovolts	Amperes	Required	Available	Current-Transformer Secondary Amperes	Normal Relay Current Amperes
63	137	172/5	200/5	4.3	—0.2
61.5	141	175/5	200/5	4.38	—0.12
60	144	180/5	200/5	4.5	0
58.5	148	185/5	200/5	4.63	+0.13
57	152	189/5	200/5	4.73	+0.23
12.2	710	888/5	1,000/5*	4.44	—0.06
12	722	902/5	1,000/5*	4.51	+0.01
11.8	734	918/5	1,000/5*	4.59	+0.09
4.32	2,004	4,340/5g	4,000/5	5.43	+0.93
4.16	2,082	4,508/5	4,000/5	5.64	+1.14
4	2,165	4,687/5	4,000/5	5.86	+1.36

g Equals $2,506 \times \sqrt{3}/5$ to compensate for the delta connection.

* Auxiliary current-transformers 15/5 amperes current step-down to compensate for the delta plus inverted delta connection.

5. The current transformers in all connections to the transformer bank must have their performance characteristics so well-matched that transient differential secondary currents may be held to a minimum at all times when no internal fault exists.

Conditions 1, 2, and 3 can usually be arranged without much difficulty. Conditions 4 and 5 are ordinarily speaking never possible to be achieved to within their closest theoretical limits.

The Current Connections

The simplest case of all is that of a single-phase two winding transformer as shown in Figure 1. The three-phase two-winding delta-delta-connected transformer bank shown in Figure 2 is quite commonplace. Another case quite as common is the wye-delta-connected transformer bank shown in Figure 3. These three cases serve to illustrate how conditions 1, 2, and 3 are usually satisfied.

It should be noted that no neutral current or ground fault current can return to the delta-connected windings of a transformer bank, except through the admittance of the windings from the cores to their conductors. This current is always of a very small magnitude and can be ignored for the purposes of studies in current-differential protection.

It should also be noted that by putting wye-connected current transformers on the delta side of the transformer bank and delta-connected current transformers on the wye side of the transformer bank, the phase angles of the secondary currents can quite easily be adjusted so that they will satisfy condition 4.

The case of the delta-zigzag transformer bank shown in Figure 4 is one of the newer ones to come up, and, as will be seen by a study of its vector diagrams, is a hybrid one. The phase relations of the high-voltage and low-voltage circuits are the same as those for a delta-delta-connected transformer bank. There is a path for zero-phase-sequence current through the neutral connection of the zigzag winding; therefore, the current transformers on this side must have their secondary circuits connected in delta. We must not shift the phase relations of the current-transformer secondary currents on the delta side, and so these current transformers must have their secondary windings connected in delta also. This differs from the arrangements shown in Figures 2 and 3.

The three-winding delta-zigzag-wye transformer bank, shown in Figure 5, presents an interesting problem. Let us look at the connections of the windings and from the foregoing discussion decide what the requirements will be for each set of windings of the transformer bank.

1. The high-voltage winding is delta-connected and, therefore, will have current transformers with wye-connected secondary windings.
2. The intermediate-voltage windings are connected in zigzag, and, therefore, the current transformers *must* be delta-connected.
3. The low-voltage windings are wye-connected, and, therefore, the current transformers *must* be delta-connected.

Now the vector diagrams for the transformer bank show that the intermediate-voltage and the low-voltage windings have their output currents 30 degrees out-of-phase with each other; therefore, they cannot ordinarily both have current transformers with delta-connected secondary circuits and at the same time have their phase angles adjusted to a balanced condition. Furthermore, the high-voltage windings and the intermediate-voltage windings have their output currents in-phase with each other, and, therefore, the phase angle must not be disturbed as would be the case with dissimilar current-transformer connections.

The high-voltage delta-connected windings, having wye-connected current transformers, will provide secondary currents which represent the phase angles of reference to which the secondary currents from the current transformers connected to the other two sets of power windings must be adjusted.

This difficulty can be overcome successfully by the arrangement shown in Figure 5. The current transformers in the leads to the zig-zag windings are connected in delta. This has provided the necessary circulating path for the zero-phase-sequence components, but it has shifted the phase angles of the currents. A set of auxiliary current transformers must now be used to shift the phase angles back again. It should be noted that normally there will be a one to three step-up in current from the secondary currents in the original current transformers to the output of the auxiliary current transformers into the differential-relay circuit. This factor and any other adjustments in ratio that may be necessary can be taken care of by selecting auxiliary current transformers of a suitable ratio.

To determine the ratio required let:

I_p = the current in amperes from the zig-zag windings of the power transformers
I_r = the current in amperes required for the current differential circuit
$R_1/1$ = ratio of the current transformers in the leads from the zigzag windings of the power transformers
$R_2/1$ = ratio of the auxiliary current transformers

Then

$$R_2 = \frac{I_p \cdot (\sqrt{3})^2}{R_1 \cdot I_r}$$

The low-voltage wye-connected windings present no difficulty. The delta-connected current transformers provide the necessary circulating path for any zero-phase-sequence components, while the phase angles of the secondary output currents are adjusted to match those from the high-voltage side.

A more complicated case is the one of the "Scott"-connected transformer bank as shown in Figure 6. It will be noticed that the worst possible condition for current-differential protection has been considered. The high-voltage circuit is three-phase four-wire. The low-voltage circuit is two-phase four-wire and also has the mid-points of the phase windings of the transformers interconnected and grounded. Here are possibilities of three-phase neutral current and also of two-phase neutral current. Obviously, the three-phase current transformers must have their secondary windings delta-connected.

The two-phase circuit presents the real problem. We have to achieve two things:

1. To arrange that the over-all ratio of the current transformers is such that the final output will be of the right magnitude for the current differential circuit.

2. To change these output currents from two-phase to three-phase.

Of course the fundamental idea must be the same as that of the "Scott" connection, but it is not so easily accomplished with current transformers as with power transformers. The transformer producing the current in the phase *W-X* is the teaser of the transformer bank. Therefore, the current transformers in the circuit *W-X* must perform the same function in the current-differential circuit, by producing the quadrature components for *B* and *C* phases as well as providing the current for *A* phase.

Three auxiliary current transformers are required: two of them must have three windings, the other needs only two windings.

The two three-winding current transformers are called phase-mixing transformers for want of a better name. As will be seen, one of them mixes phases *W-X* and *Y-Z* to produce a resultant current for *B* phase. Similarly, the other one mixes phases *W-X* and *Y-Z* to produce a resultant current for *C* phase. The difference between the two resultants is achieved by reversing the direction of the current *Y-Z* in one of the phase-mixing transformers.

The two-phase current transformers have no interconnection between phases *W-X* and *Y-Z*. Therefore, any two-phase neutral current components when passing through the auxiliary current transformers will produce three-phase zero-phase-sequence components in the output circuits.

By connecting the output windings of the auxiliary current transformers in delta, a circulating path is provided for the zero-phase-sequence components, and the phase angles of the output currents are adjusted to match those from the three-phase side.

Let us now get on to the determination of the current transformers required.

Let

I_h = the current in amperes in the three-phase side of the power transformers
I_l = the current in amperes in the two-phase side of the power transformers
I_{w-x} and I_{y-z} = the currents in amperes in the respective current-transformer secondary windings on the two-phase side
I_r = the current in amperes required for the current-differential circuit
$R_1/1$ = ratio of the current transformers in

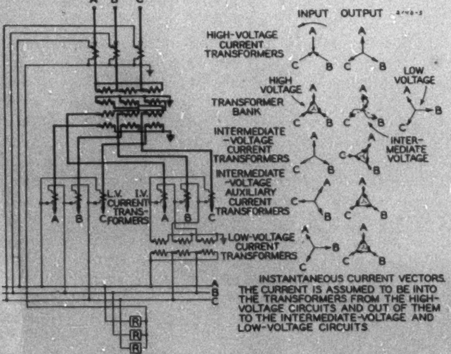

A
B
C
INPUT
OUTPUT
HIGH-VOLTAGE CURRENT TRANSFORMERS
HIGH VOLTAGE
TRANSFORMER BANK
LOW VOLTAGE
INTERMEDIATE-VOLTAGE CURRENT TRANSFORMERS
INTER-MEDIATE VOLTAGE
INTERMEDIATE-VOLTAGE AUXILIARY CURRENT TRANSFORMERS
L.V. CURRENT TRANS-FORMERS
I.V.
LOW-VOLTAGE CURRENT TRANSFORMERS
R
R
R
INSTANTANEOUS CURRENT VECTORS.
THE CURRENT IS ASSUMED TO BE INTO THE TRANSFORMERS FROM THE HIGH-VOLTAGE CIRCUITS AND OUT OF THEM TO THE INTERMEDIATE-VOLTAGE AND LOW-VOLTAGE CIRCUITS

Figure 5. Three-phase delta-zigzag-wye transformer-bank differential protection

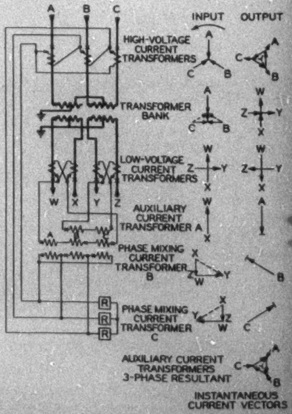

A
B
C
INPUT
OUTPUT
HIGH-VOLTAGE CURRENT TRANSFORMERS
TRANSFORMER BANK
LOW-VOLTAGE CURRENT TRANSFORMERS
W
X
Y
Z
AUXILIARY CURRENT TRANSFORMER A
PHASE MIXING CURRENT TRANSFORMER B
PHASE MIXING CURRENT TRANSFORMER C
R
R
R
AUXILIARY CURRENT TRANSFORMERS 3-PHASE RESULTANT
INSTANTANEOUS CURRENT VECTORS

Figure 6. Three-phase to two-phase "Scott"-connected transformer-bank differential protection

Table II

Full Load of Transformer Bank = 5,000 Kva 3-Phase–2-Phase
Current-Transformer Ratios Figured for 25 Per Cent Overload. Base 60 Kv

Kilovolts	Amperes	Phase	Current-Transformer Ratios: Required	Current-Transformer Ratios: Available	Current-Transformer Secondary Amperes	Normal Relay Current Amperes
63	46	3	100/5[g]	100/5	5	−0.2
61.5	47	3	102/5	100/5	5.1	−0.1
60	48	3	104/5	100/5	5.2	0
58.5	49	3	106/5	100/5	5.3	+0.1
57	51	3	110/5	100/5	5.5	+0.3
6.86	364	2	455/5	$\frac{1,000}{2}/5$†	4.55*	−0.65
6.6	379	2	474/5	$\frac{1,000}{2}/5$†	4.74	−0.46
6.34	394	2	493/5	$\frac{1,000}{2}/5$†	4.93	−0.27

[g] Equals $58\times\sqrt{3}/5$ to compensate for delta connection.

* Three-phase current the same value after passing through phase-mixing current transformers.

† Two 1,000/5 current transformers with secondary windings in parallel = 500/5.

the three-phase leads of the power transformers

$R_2/1$ = ratio of the current transformers in the two-phase leads of the power transformers

$R_3/1$ = ratio of the auxiliary current transformer for *A* phase

then

$$R_1=\frac{I_h\cdot\sqrt{3}}{I_r}$$

$$R_2=\frac{I_l\cdot 2}{I_{w-x}} \text{ or } \frac{I_l\cdot 2}{I_{y-z}}$$

and

$$R_3=\frac{I_{w-x}\cdot\sqrt{3}}{I_r}$$

Now when $I_{w-x}=I_{y-z}=I_r$

then the current input for *B* and *C* phases equals

$$0.866I_{y-z}+j0.5I_{w-x}$$

and the ratios of the phase-mixing transformers when expressed in terms of amperes will be

$$1.154I_{y-z}/2I_{w-x}\bigg/\frac{I_r}{\sqrt{3}}$$

This expression will hold good for any values of I_{w-x}, I_{y-z}, and I_r, even if they are not equal.

Generally speaking, a transformer differential protection should cover not only the transformer bank but as much more of the station equipment as possible. In Figure 7 there is shown an arrangement which can be considered as typical. Here there is shown a three-winding transformer together with its necessary circuit breakers and connections which are all included under the one differential protection. A fault anywhere in the circuits between the transformer and the six groups of current transformers, including the transformer itself, will cause the differential relays to operate. In large stations the bus bars indicated and the circuit breakers connected to them may be separated by considerable distances, so that the connections may include quite long pieces of cable. The differentially protected zone would then reach out to cover equipment situated all over the station installation.

Disturbing Factors

The foregoing discussion of the current-transformer circuits is based on the assumption that all instruments and machines with magnetic cores behave absolutely normally at all times. They do so only after the currents have reached their steady state. Unfortunately, they behave very abnormally during the times that transformers or machines are being cut in and out of service, and also during the times of heavy faults anywhere on the system which can greatly disturb the current conditions in any particular protected zone.

The most usual abnormal conditions are referred to as

1. Through-current transients.
2. Transformer charging-current transients.

Through-current transients are caused by faults which draw very heavy currents through the transformer bank, and the transient magnetic characteristics of the circuit are such that, for a few cycles, the current transformers on one side of the transformer bank do not deliver secondary currents which will balance those from the other side.

Transformer charging-current transients are caused by the inrush of the charging current of the transformer bank at the instant that the first circuit breaker closes. Obviously, the current must appear in the current transformers on one side of the transformer bank only when only one circuit breaker is closed. Fortunately this current persists for only a few cycles also.

Without discussiong these disturbing factors in further detail, let us say that the problem that confronts one from both of these conditions is to get the relays to ignore these transient currents and still remain sensitive enough and fast enough to clear any faults in their zone of protection before excessive damage can result from them.

Here again the war conditions enter into the picture. Old transformers often take very high charging inrushes of current. Current transformers of older types often have saturation curves and d-c time constants which differ widely from those which are built today. In fact, many of them were built when little attention was paid to standardization of performance. Therefore, one may be forced by circumstances to make use of current transformers which have characteristics differing very widely one from another. These conditions are almost sure to produce excessive current-transformer transient differential currents.

There are quite a few types of relays on the market which are arranged to be restrained during transient conditions and are designed on very sound theoretical bases for the purpose. Unfortunately, these relays are quite expensive to put into temporary installations. Also it is becoming more and more difficult to obtain relays of ordinary standard types, let alone those which are of quite special types, as these restrained relays are.

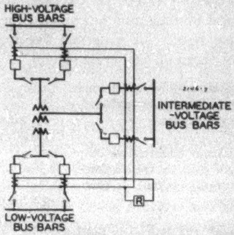


Figure 7. Grouping of current transformers for transformer-bank zone differential protection

Single-phase connections shown

Tolerable Differential Currents

There are some compensating factors which help to simplify the problem. It is quite obvious that insofar as the relays are concerned, either the transformer charging current, or the through transient current will produce the greater differential current. Therefore, if we can restrain them for the greater current, we shall have no trouble with the smaller one.

In case of a fault in the protected zone, the damage done will be a function of the time that the fault persists and of the watts that are poured into it. If the impedance to a fault be high, and the power fed into it be therefore low, one may stretch out the time it may be allowed to persist without doing any excessive damage.

It is quite usual in practice to find that current transformers cannot be arranged so that the differential current through the relays is zero at all times. There will almost always be some permanent differential current flowing which will be added to the true differential current under fault conditions. Insofar as the relays are concerned, this permanent differential current is the same as a permanent fault of very high impedance. A transient differential current will be the same as a transient fault, usually of relatively high impedance.

The Relays

Let us assume that one has not much choice of the current transformers and relays that one can obtain for any given differential protection. Let us assume also that, in a hypothetical case, mathematical investigation has yielded the following results for a normal five-ampere relay circuit on a 60-cycle system.

Maximum differential current due to through-current transients —12 amperes for 20 cycles

Maximum differential current due to transformer charging-current transients —15 amperes for 15 cycles

Normal 125 per cent load differential current through relays —2 amperes

Maximum fault differential current under minimum system generating capacity —5 amperes

If we can obtain both time delay and instantaneous relays of suitable types, we can adequately protect the transformer bank by using one instantaneous and one time relay per phase and set them as follows:

Instantaneous relay: Pickup at 20 amperes
Time delay relay: Pickup at 2.5 amperes
Operate at 14 amperes in 30 cycles or at 17 amperes in 23 cycles.

The actual setting of the time-delay relay will depend upon its inherent characteristics. It must be determined by test. Let us suppose that for a given relay setting we get the following performance:

Operate at 14 amperes in 30 cycles.
Operate at 17 amperes in 15 cycles.

and for another setting we get

Operate at 14 amperes in 35 cycles.
Operate at 17 amperes in 23 cycles.

Then the second setting should be the one to be selected, because it gives ample time clearance for both through-current transients and transformer charging-current transients. The first setting comes too close to the anticipated time for transformer charging-current transients to persist and might result in false operations.

Almost always the fault current will be high enough to operate the instantaneous relay, but the time-delay relay is necessary to provide against faults at times when the short-circuit current to the fault would be sharply limited by the system setup. Transient currents will not operate the instantaneous relays, because they cannot reach a high enough value. They may, however, start the time-delay relays in operation, but they will die out before the relays can close their contacts. The low value of pickup current of the time-delay relays—0.5 amperes above the normal 125 per cent load differential current—will ensure that they will operate under minimum fault conditions and will not pick up under normal conditions. It is true that the time will be long in clearing, but the limited power available at the fault will limit the extent of the damage that can be done. Most manufacturers of relays now combine these two types of relays in one case.

In some places it is customary to use some sort of additional suppresser circuit to handle transient conditions. The suppresser circuit is usually arranged to shunt away part of the transient differential current from the usual percentage differential type of relays and slow up their action until the transient conditions have passed. This arrangement has one great disadvantage. There is no instant of time when a fault may show up in a transformer bank or its connections in the protected zone more than the one when the first circuit breaker closes and the charging current rushes in. A suppresser circuit makes the relays far less sensitive while it is in operation than they would be normally. Therefore, any fault developing before the suppresser circuit has timed itself out will require a much greater fault current to operate the differential relays than would otherwise be necessary. At times with low generator capacity in service this may cause a complete failure of the differential protection. Some suppresser circuits, which operate on straight voltage for their timing, could never time out during heavy faults in the protected zone, due to the voltage around the zone falling to a very low value, perhaps almost to zero.

On the whole, considerable tolerance can be allowed in the selection of current transformers and relays for current-differential schemes as will be seen from Tables I and II. Table I is worked out for a delta-zigzag-wye transformer bank such as the one shown in Figure 5. Table II is worked out for a "Scott"-connected bank as shown in Figure 6.

While it is always better to have instruments such as relays perform a function for which they have been especially designed, there are often quite a few applications for relays of certain types which were not contemplated at the time the relays were originally thought of. Fortunately, ordinary overcurrent relays can, with care, be made to suffice quite well for transformer differential protection, if time and money are important factors in getting the installation into service.

Conclusions

The most important and, in fact, the essential thing to make sure of in the design of a current-differential protective circuit is that the instantaneous directions of the various currents which must be matched are right and that they cancel out fairly well at the relays. This requirement naturally presupposes that there will be no phase-angle shifts between the currents coming into the relay for any one phase at any time. The exact balance of the magnitudes of the various currents is not very critical. The exact matching of the characteristics and ratios of the current transformers, which are used, is not necessary under ordinary circumstances. It may be necessary where unusually high sensitivity is required. While special types of relays for this work are normally available and give good results, it is not essential that one have anything but some reliable instantaneous and time-delay overcurrent relays in order to provide adequate protection in most cases. Necessary phase-angle shifts of currents, in order to adjust the instantaneous directions of the currents reaching the relays, can be made with auxiliary current transformers. These should have as low an internal burden as possible, in order not to upset the ratios of the power current transformers with which they are connected during heavy current periods. In current transformer circuits connected to transformer windings which have a return path for current through a neutral connection, a delta circulating path for the zero-phase-sequence components in the phase leads must be provided.

Some Air-Blast Circuit-Breaker Installations in Canada

H. W. HABERL
ASSOCIATE AIEE

R. A. MOORE
NONMEMBER AIEE

THIS paper gives a brief report on certain installations of air-blast circuit breakers now in service in Canada, including results of interrupting tests and operating experience.

There are in service in Canada 23 air-blast breakers of 69 kv, 138 kv, and 230 kv of the types illustrated. There are 37 breakers in service on 13 kv and 4 kv. These breakers are in service in ten different stations in various localities.

Figure 2 illustrates an installation of two 230-kv breakers in a station, carrying a load of about 250,000 kva per breaker. Two identical breakers are in service in another city.

Figure 4 shows six 138-kv breakers in a switchyard built for ten air-blast breakers. This switchyard was built in 1934, and 138-kv air-blast breakers have been in service continuously since that time in this installation.

Figure 5 shows three 138-kv air-blast breakers in a city station of 135,000-kva transformer capacity.

In Figure 6a is shown a 138-kv breaker in a downtown city station of 80,000-kva transformer capacity, fed by 120-kv underground cables. This station is built for air-blast breakers exclusively and will have an ultimate installation of 24 15-kv and 36 4-kv breakers.

Figure 7 shows one of two 110-kv breakers at a generating station in the north country (above latitude 48).

Air-blast breakers of 69-kv design are shown in Figure 8, in service in a city station of 40,000-kva transformer capacity. In Figure 11 is shown one of five 69-kv breakers in a terminal station where the short-circuit duty is 1,100,000 kva, this being the recorded interruption on test of one of these breakers in this station.

Air-blast breakers in service on 11-kv generator voltage are shown in Figures 13 and 14. Figure 15 shows a group of seven 4-kv breakers now in service in Western Canada.

Interrupting Tests

The type of 230-kv breaker illustrated has been tested at the station where these breakers are in service, located near the center of three large power systems operating in parallel with a combined generating capacity of about 2,750,000 horsepower. The maximum short-circuit duty at this location is 2,500 amperes at 230 kv or 1,000,000 kva, which is the recorded interruption on test of this breaker as shown in oscillogram, Figure 3.

The breaker is rated at 2,500,000 kva or 6,250 amperes at 230-kv three-phase, an amount of short-circuit current not available for test in Canada. However, this breaker has six interrupting elements in series per pole, and tests have been made on one of these elements interrupting 5,385 amperes with 58.8 kv across the element (see oscillogram Figure 6). This test voltage, 58.8 kv, is about 2.67 times the voltage impressed on each of the elements when interrupting a short-circuit current (assuming line-to-neutral voltage across each pole and equal voltage across each of the six elements in series). If the voltage across one pole is assumed to be 1.5 times line-to-neutral voltage, then the above ratio of 2.67 would become 1.78; that is (under the last assumption) the single element was tested at 1.78 times its rated voltage and interrupted 5,385 amperes which is 86 per cent of 6,250 amperes, its rated current. Multiplying 1.78 by 0.86 gives 1.54; that is, the test is an indication that the breaker would interrupt 6,250 amperes with a voltage across one element 1.54 times the normal (equally divided) voltage, and with a voltage across the same pole of 1.50 times the line-to-neutral voltage. It will be noted that the only extrapolation in the above is from 5,385 amperes test current to 6,250 amperes rated current, that is, an assumption of the same kilovolt-ampere capacity at 6,250 amperes. The assumption of 1.5 times line-to-neutral voltage across one pole is one which has been commonly used, and the assumption of 1.54 times the equally divided voltage across one element is believed to be more conservative than necessary. The oscillogram shows that the arc voltage (six arcs in series) is so low that it could be applied all across one element and be only a small fraction of 22 kv (the equally divided voltage across one element). Hence, if there is any practical inequality of voltages across the six elements, it could only occur after the interruption (and until the opening of the series isolating switch, while the blast of air is still passing between the contacts). The electrostatic field is quite uniform, being determined by the metal roof 30 by 48 inches, and a metal bedplate of the same size, 60 inches below it, supported on insulator stacks, about seven feet above the grounded structure. The insulating structure housing the contacts and arc chutes has a large horizontal cross section. The six sets of contacts occupy identical locations in this structure. Such conditions are conducive to uniform division of potential across the contacts.

Tests on the 69-kv breaker (oscillogram Figure 9) also have a bearing on the interrupting capacity of the 230-kv breaker. The interrupting element in the 69-kv breaker is the same as in the 230-kv breaker, except that the width of the 69-kv contact (and arc chute) is eight inches instead of five inches as in the 230-kv breaker. The 69-kv breaker has interrupted 10,000 amperes on a three-phase short circuit on a 63-kv circuit, which test subjected the single element of the 69-kv breaker to a transient voltage of

Figure 1. The first 138-kv air-blast breaker of this type

In service from 1934 until 1940

Paper 42-157, recommended by the AIEE committee on protective devices for presentation at the AIEE Pacific Coast convention, Vancouver, British Columbia, Canada, September 9–11, 1942. Manuscript submitted June 3, 1942; made available for printing July 24, 1942.

H. W. Haberl is protection and relay engineer, Montreal Light, Heat, and Power Consolidated, Montreal, Quebec, Canada.

R. A. Moore is switchgear engineer with the English Electric Company of Canada, Ltd.

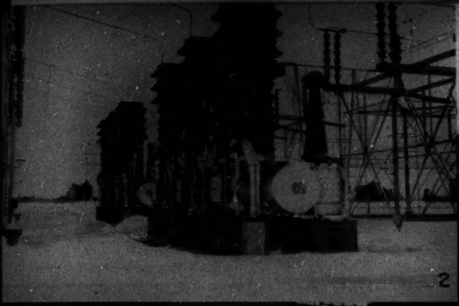


Figure 2. Two 230-kv air-blast circuit breakers in service on long 230-kv lines

One breaker carrying approximately 250,000-kva load. Four of these breakers in service in Canada

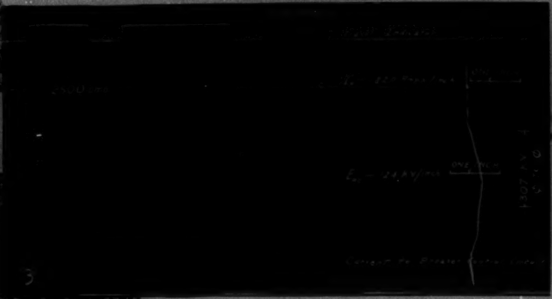


Figure 3. "Make-break" test on 230-kv breaker on 230-kv line, interrupting maximum current available 2,500 amperes

Arcing time one-half cycle (see text for additional tests showing interrupting capacity above 2,500,000 kva)

Figure 4. Switchyard built for ten 138-kv air-blast breakers

Six have been installed, controlling 200,000-kw load

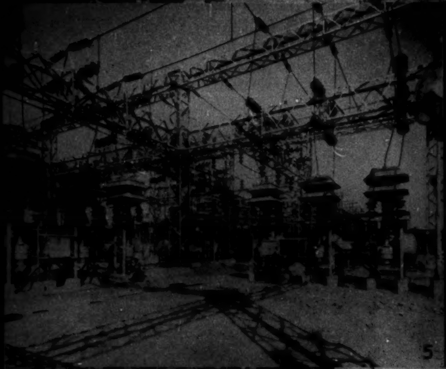


Figure 5. Receiving station switchyard with three 138-kv air-blast breakers installed, operating on 150 pounds per square inch

Closing time 13 cycles—opening time 3 cycles

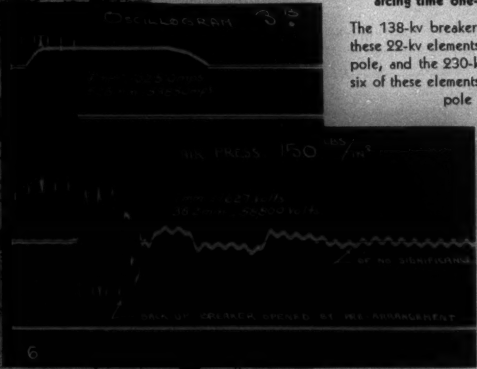


Figure 6. "Make-break" test applying 58.8 kv across one 22-kv interrupting element, interrupting 5,385 amperes, arcing time one-half-cycle

The 138-kv breaker has three of these 22-kv elements in series per pole, and the 230-kv breaker has six of these elements in series per pole

Figure 6a. 138-kv breaker on an underground 120-kv cable-fed substation located in a downtown area

This breaker is within ten feet of a busy street

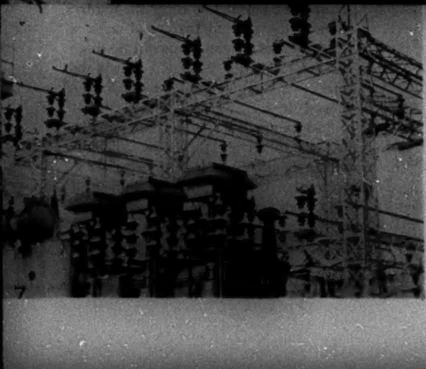


Figure 7. Two 110-kv breakers at generating station in northern Quebec

These breakers have two interrupting elements in series per pole. Addition of one interrupting element per pole would increase voltage rating to 138 kv and interrupting capacity to 2,000,000 kva

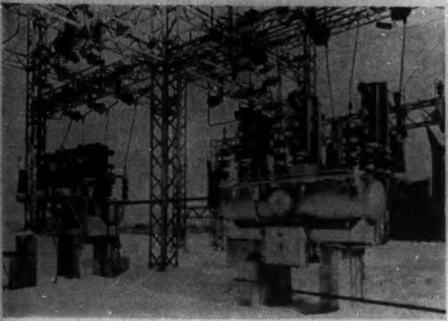


Figure 8. Two 69-kv breakers in city station

Switchyard laid out for nine of these breakers. Each pole has one interrupting element eight inches wide; otherwise the same as the elements in 138-kv and 230-kv breakers, which are five inches wide

36.4 kv. Reducing 36.4 kv in the ratio of five inches to eight inches gives 22.75 kv as the voltage at which the five-inch element should interrupt 10,000 amperes. If the voltages are divided equally in the 230-kv interruption, each element would be subjected to only 22 kv, but assuming 1.5 times line-to-neutral voltage across one pole, then one element would be subjected to 33 kv. Reducing 10,000 amperes interrupted on test, in the ratio of 22.75 kv to 33 kv, gives 6,900 amperes as the interruption at 230 kv, which is 2,750,000 kva. In this case we are interpolating, from the higher test current of 10,000 amperes to the lower current (6,900 amperes) which would justify a slightly higher kilovolt-ampere rating than we have assumed in the voltage ratio calculation.

It will be noted that none of the tests on this style of interrupter showed what is the maximum interrupting capacity. Each of the tests was simply made at the maximum current and voltage available, and none of the tests indicated that the interruption was near the limit of capacity of the breaker.

The 138-kv breakers illustrated have identically the same interrupting elements as the 230-kv breakers, with three elements in series per pole (instead of six). The tests above referred to are, therefore, directly applicable to the 138-kv breaker. The current-interrupting rating is practically the same (6,250 amperes), and the voltage per element is practically the same.

Tests were made also on the 69-kv breaker by applying 58.8 kv across one pole, interrupting 5,580 amperes, which was the maximum current available. The significance of these tests is the liberal overvoltage capacity shown (see oscillogram Figure 10).

In general the tests on all of the breakers above referred to have shown arcing time of one-half cycle. The contacts have shown negligible burning and did not require cleaning up before putting them in service. The arc did not extend outside of the cooling grid. All of the tests were "make-break" and the disconnecting switch contacts making the short circuit performed satisfactorily (the motion of the switch blade being very fast), the arc was not objectionable, and the contacts did not require cleaning up.

The tests were witnessed by disinterested visiting engineers from various power companies, and there was general agreement that no distress was shown in the interruptions.

Operating Experience

Service experience with air-blast breakers in Canada (and the United States) dates from 1934 when the first 138-kv breaker was put in service in the switchyard of a large generating station. It remained in service continuously for about six years interrupting all short circuits satisfactorily. Its isolating switch was very slow, and the workmanship on its interrupting contacts was relatively crude, finally resulting in overheating while carrying load, and it was superseded by breakers of improved design and workmanship. Breakers of the later style have been installed in the same station, and there are now six installed. This first installation demonstrated that there were no serious difficulties in operating and maintaining air-blast breakers of that voltage outdoors in a location subject to climatic extremes of moisture and temperature. The activated alumina twin air dryers installed in that station in 1934 are still in satisfactory operation serving six breakers. The early experience showed the importance of extracting the oil vapor as well as the moisture from the compressed air. There was much improvement made in the detailed technique of determining when the alumina needed reactivating; the color-changing alumina was a practical help, but not essential; the experienced operators got along quite well with the plain white product. The oil vapor (from the lubrication of the compressor pistons) requires a good tight woolen blanket to filter it out. If the blanket does not fit tightly in the container, the oil vapor will blow by. If oil vapor is allowed to pass into the breaker valves (especially the pressure-reducing valve), it will cause the small parts to stick. Consideration has been given to the use of oilless compressors, using carbon piston rings, but the maintenance of the carbon rings and extra cost of such compressors does not appear attractive nor justified. The

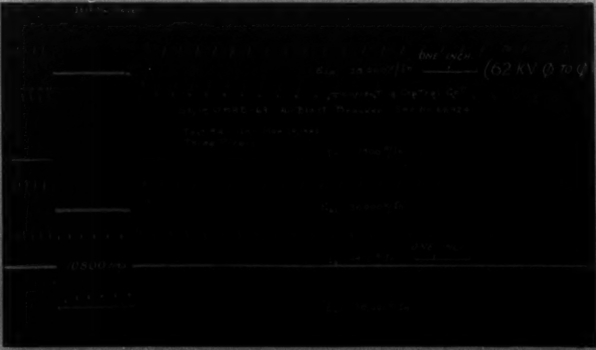


Figure 9. Three-phase "make-break" test on 69-kv breaker in 63-kv circuit, interrupting maximum current available 10,000 amperes, equals 1,090,000 kva

Opening time 3³/₄ cycles, including one-half-cycle arcing (see text for additional tests indicating higher interrupting capacities)

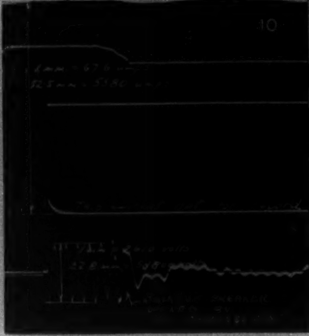


Figure 10. "Make-break" test applying 58.8 kv across one 40-kv interrupting element of 69-kv breaker

Interrupting maximum current available, 5,580 amperes, one-half cycle arcing

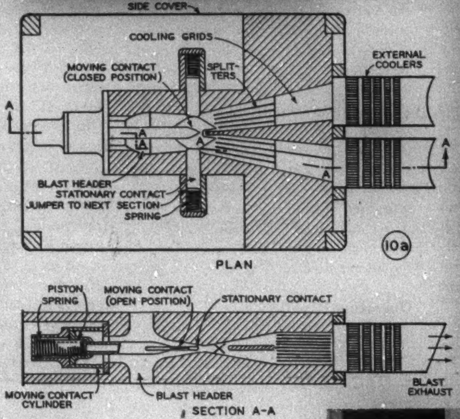

SIDE COVER
COOLING GRIDS
MOVING CONTACT (CLOSED POSITION)
SPLITTERS
EXTERNAL COOLERS
A
A
A
A
A
BLAST HEADER
STATIONARY CONTACT
JUMPER TO NEXT SECTION
SPRING
10a
PLAN
PISTON SPRING
MOVING CONTACT (OPEN POSITION)
STATIONARY CONTACT
MOVING CONTACT CYLINDER
BLAST HEADER
BLAST EXHAUST
SECTION A-A

Figure 10a. Details of interrupting element

Two elements in series on 110-kv breaker, three elements in series on 138-kv breaker, and six elements in series on 230-kv breaker

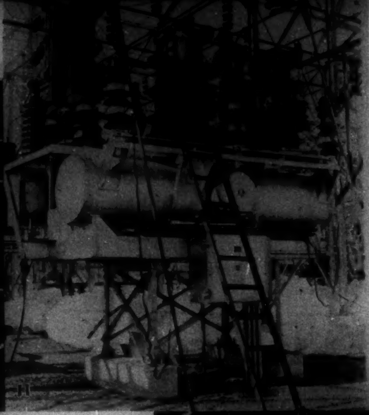


Figure 11. 69-kv breaker 20 minutes after short-circuit test

One pole completely taken down, and inspection complete, showing ease of inspection

Figure 12. "Inside information" on a 69-kv air-blast breaker after interrupting four short circuits at or above rated capacity

Showing ease of inspection and minimum burning. Moving contact pulled down for inspection

woolen filter has been found quite satisfactory.

There are now in service in Canada 23 air-blast breakers of 69, 138, and 230 kv, in seven different localities. Their service record is equivalent to about 21 "breaker years."

In one instance certain new-style arc-chute details were installed without sufficient interrupting tests, because of the desire to meet promised shipping dates, and certain new parts were found necessary after the tests. In other instances, water had accumulated in the air piping and was not blown out before connecting the breaker, resulting in a flashover in one of the small porcelain "closing" tubes, requiring only replacement of the tube. Aside from such experience, the straight work of installing air-blast breakers does not require any greater skill or labor than installing oil breakers; in fact, the absence of any mechanical linkage between poles of the air-blast breakers eliminates the close mechanical adjustments required in installing oil

Figure 13. Delle breakers installed in a large power-house

Main generator and transformer breakers operating on 11 kv rated 1,000,000 kva. This station is completely equipped with air-blast breakers

14

Figure 14. Factory picture, English Electric Company (Canada) of Delle breakers for four-kilovolt service

These breakers are now in service in western Canada breakers. Also there is no item in the air-blast job corresponding to the job of filtering large quantities of oil during installation of oil breakers.

In the 69-kv, 138-kv, and 230-kv breakers herein illustrated the contacts and other parts requiring occasional inspection are very accessible. The contacts can be removed and replaced in a small fraction of the time required for corresponding inspection of oil-breaker contacts.

In these breakers the interrupters are supported on standard switch-type insulators (the same as used for bus supports and disconnecting switches). The breakage of the porcelain on such supports does not impair their mechanical strength, and there is no danger of the interrupter falling.

In the experience of several years of testing various styles of interrupter contacts and arc chutes to determine their interrupting limitations, the observers have been impressed with the fact that a failure to interrupt does not result in serious damage. After such a failure to interrupt, the contacts require resurfacing or replacement of arcing tips, certain parts of the arc chute require cleaning up or replacement, but the remainder of the breaker remains intact.

The 138-kv and 230-kv breakers illustrated are readily adaptable to single-pole operation (automatically opening and reclosing one pole only to clear a line-to-ground fault), because there is no mechanical connection between the three poles. The 69-kv breakers have mechanical connection, but this can readily be dispensed with in case single-pole operation is desired.

The opening time (including arcing time) of the 138-kv breaker is within three cycles. The other two breakers take about three quarters of a cycle longer in opening. This can be shortened by about one cycle in the case of the 69-kv breakers by omitting the mechanical tie between the poles. The opening time is also influenced to some extent by the adjustment of certain small valves controlling the opening and closing of the blast valve, so that the opening time of the blast valves and the breaker can be somewhat reduced below the above figures.

Regarding closing time, the breakers as now adjusted take about 12 cycles to close in the case of the 69-kv breaker, about 15 cycles for the 138-kv, and about 19 cycles for the 230-kv. This time is also susceptible of reduction by certain adjustments.

In cases where extremely fast reclosing is desired, this can be arranged by certain adjustments which advance the time of shutting off the blast and applying closing air pressure, it being unnecessary to wait until the isolating switch has completed its opening motion.

It has been noted that all of the tests recorded by the oscillograms were "make-break" interrupting tests. The construction of these breakers is such that the opening and clearing time is the same whether the operation is a "make-break" or a "break" performance, as the breakers are provided with pneumatic tripfree valves.

As shown by the illustrations and remarks concerning the 138-kv breaker, even after one of these breakers has been installed and operated, if changes should be made in the system either by increasing the voltage or increasing the interrupting duty, it is a relatively simple matter to add one or two supporting insulators and one or two interrupting sections, thereby increasing the voltage and interrupting capacity of the breaker without discarding any of the parts. For instance, a system might initially operate at 110 kv and in later years be raised to 138 kv or 180 kv by the additions to the breaker as indicated.

The same remarks apply to the 230-kv breaker in regard to a possible increase in voltage from 230 kv to 280 kv for instance, which could be accomplished by adding one or two supporting insulators.

Low-Voltage Air-Blast Breakers

Figures 13, 14 and 15 show installations of 15-kv and 4-kv air-blast breakers in Canada. The first few breakers of this type were imported from the Delle Company in France. The other breakers of the same design were built by the English Electric Company of Canada. The interrupting capacity has been established by the tests made in the testing laboratories in France and by certain tests made in the testing laboratories of the I-T-E Circuit Breaker Company in Philadelphia. Further tests have been made on these breakers at the stations where they are installed. Certain improvements in detail have also been made by the Canadian company to meet Canadian requirements.

The 15-kv breakers illustrated have been operating satisfactorily for about two years on 11 kv in a large generating station which is equipped with air-blast breakers exclusively.

References

1. Mechanical Simplicity of Air-Blast Breakers, H. W. Haberl, Otto Jensen. AIEE Transactions, volume 60, 1940, September section, page 869.

2. Discussion by H. W. Haberl of Circuit Interruption by Air Blast. AIEE Transactions, volume 59, 1940, page 1129.

History of A-C Wave Form, Its Determination and Standardization

FREDERICK BEDELL
FELLOW AIEE

Synopsis: With the birth of the transformer and the first distribution of alternating currents, wave form assumed interest, and methods were developed for its determination, chiefly the point-by-point method of instantaneous contact, mechanical oscillograph, cathode-ray oscillograph, and the oscilloscope with stabilized time axis. The point-by-point method, by which were made the first major contributions, is now practically superseded by oscillograph and oscilloscope, each finding increasing use in its field.

With the determination of wave form accomplished, demand arose for its standardization corresponding to expanding applications. No single standard being suited to all applications, different standards have been developed in different fields, as in power, communication, and insulation. While it is desirable that standards, once set up, remain fairly stable, they should be subject to review and occasional change to keep in step with technological advances. Minor revision in communication is in progress. Although standards in other fields do not appear ideal, no immediate revision is recommended. Forty references are appended.

Alternating Current in the Late 80's

THE distribution of alternating current as we now know it began in 1885–86. Previous to that time, alternators were used to operate arc lamps in lighthouses, each machine operating one large arc lamp, but there was no distribution. Late in 1885, William Stanley made his first constant potential transformer, and early in 1886, using six transformers for operation in parallel distributed current at Great Barrington, Mass., to a score of customers at a distance of 4,000 feet. The secondary voltage was 100 volts. Current was supplied from a Siemens alternator, designed for 12 amperes 500 volts, imported for the purpose. At the time of this first installation, it was pointed out by electricians that "if a high potential primary circuit of 500 volts or more were used to distribute electricity throughout a community, there was a grave fire and life danger." A few years later a bill was introduced[1] but not passed in the Virginia legislature to limit a-c pressure to 200 volts, alternating current being considered (by those opposing its use) to be more deadly than direct.

Stanley's installation at Great Barrington was put in regular service in March 1886, and operated until summer, when an attendant dropped a screw driver into the alternator and ruined it. Meanwhile, on April 6, 1886, George Westinghouse, accompanied by W. L. Church, F. L. Pope, W. C. Kerr, and others, had seen the system in operation and determined to actively enter the a-c field. The manufacture of a new type of alternator designed by Stanley, the radial-pole type thereafter generally used, was undertaken in Pittsburgh. The story is well told by Stanley[2] himself with interesting side lights with credit to others.

The following winter the first commercial installation was made at the station of the Buffalo Electric Company. The question of the determination of a-c wave form then arose as an engineering problem. Before considering wave form, we should note the meagerness of knowledge then possessed concerning the behavior of alternating currents.

In the late 80's many misconceptions impeded progress. Series *versus* parallel operation of transformers was much discussed without adequate understanding. In 1886, at the very time that the parallel operation of transformers was successfully accomplished as we have just seen, a distinguished cantor lecturer held such a connection to be impracticable, maintaining that a separate lead to each transformer would be necessary, or each transformer should have a special regulating apparatus.

Referring to constant current operation with transformer primary windings in series, the chief of an electrotechnical testing station explained:[3] "When no secondary current is flowing, the electromotive force in the primary and secondary coils is a maximum. (Quite correct.) We have consequently this disproportion that *the smaller the output of the apparatus the greater the energy consumed.* With the secondary circuit open, and a constant exciting current, the energy used could be as much as ten times as great as under full load." Power factor was overlooked or unknown.

The proponents of the series connection of transformers operated with constant primary current reversed the argument with the same misconception. They held that inasmuch as primary electromotive force decreased with increase of secondary load, power input also decreased (primary current being constant), with the happy result that *as more power was taken out, less power was put in*—surely a condition to be desired. Here again, power factor was neglected or unknown. Yet, without meters for measuring power, power factor or phase, such misconceptions should be expected; they merely reflected the state of the art at the time.

The United States Patent Office had shown a like misconception when in 1883 it refused[4] to grant a patent on a transformer on the ground that it would be impossible to get a larger current out of the secondary winding than was supplied to the primary winding, but in 1886 a patent was allowed for this very thing.

In the Early 90's

Many of the misconceptions of the 80's trailed along into the early 90's. In the field of alternating currents there was a growing collection of isolated facts, some understood and others not, but there was no broad foundation on which to stand. This was the situation, as recalled by the author in 1890 and 1891, when he first became interested in alternating currents and in wave form. The open-magnetic-circuit transformer was still discussed. The merits of the "nonpolar" transformer with its closed magnetic circuit was not yet fully recognized. The capacity "effect" with current before the electromotive force that produced it (the effect before the cause) and the Ferranti "effect," with electromotive force received at the far end of a cable greater than the electromotive force applied, were found baffling.

Two "systems" of a-c distribution were then in use, apparently well-established: the "high-voltage" *constant potential* (Westinghouse) system operating transformers with primary circuits of 1,000 volts and 2,000 volts, with secondary circuits wound for 50 volts and 100 volts for supplying incandescent lamps in parallel;

Paper 42-152, recommended by the AIEE committee on basic sciences for presentation at the AIEE Pacific Coast convention, Vancouver, British Columbia, Canada, September 9–11, 1942. Manuscript submitted June 17, 1942; made available for printing July 21, 1942.

FREDERICK BEDELL is professor emeritus of physics, Cornell University, Ithaca, N. Y., and consulting physicist, R. C. Burt Laboratory, Pasadena, Calif.

The author is indebted to A. C. Crehore, his first co-worker, and to later co-workers to some of whom reference is made; to L. F. Blume of Pittsfield, O. B. Buckley and H. S. Osborne of New York, L. W. Chubb and R. D. Evans of Pittsburgh, who furnished material and references; and to the staff of the California Institute of Technology.

and the *constant current* system, about 10 or 15 amperes for supplying arc lamps in series. Transformers were limited to small sizes, the dictum from high sources that transformers larger than two kilowatts would not be economical being generally accepted. Higher voltages and the use of a-c motors were in the offing. The attempted use of iron wire for a-c transmission has been reported.

It was in 1890–91 that P. N. and L. L. Nunn installed the first commercial transmission of alternating current for power, to operate a 3,000-volt synchronous motor at the Gold King Mine at an altitude of 11,500 feet near Telluride, Colo., from water power 3,000 feet lower at a distance of three miles. The situation at that time was vividly described by P. N. Nunn in an address (mimeographed) before AIEE Los Angeles section, May 8, 1934, on "Early Experiences in the Power Industry," from which the following is quoted:

> "In spite of its shortcomings, alternating current seemed the most feasible. A pair of conventional alternators were installed, one as generator, the other as motor, identical to assure identical 'wave forms,' *whatever that might be.*
>
> "In 1890 alternating current was just plain *freak;* it did not follow Ohm's law and 'clogged' itself in its circuits. Bedell and Crehore had doped out its laws and demonstrated their concepts in 100 pages of solid calculus.
>
> "Wattmeters had not been developed, nor had the term 'power factor' been adopted into the vernacular."

Evidently wave form, power factor, and the clogging effect of impedance were dimly discernible. The need of a standard wave form, "whatever that might be," was thus early recognized. From this first power transmission it was a far cry to the 287,500-volt transmission with its magnificent equipment at Boulder Dam today. The increase from 3,000 volts came slowly with gradual increases in demand for more and more power from greater distances, with notable jumps to 33,000, 40,000, and 60,000 volts. With high voltages and long distances arose problems of insulation and interference, in which questions of wave form play such important part.

Going back to horse-and-buggy days, with constant-current and constant-potential systems both in use, the question arose as to whether incandescent lamps and arc lamps could be supplied from the same system. Early in 1891 a promoter, an ardent believer in the future of the constant-current system,* sought the aid of A. C. Crehore and the author in the development of a constant-current to constant-potential transformer for operating incandescent lamps from such a system. Easier said than done, but with the enthusiasm of youth, the problem was tackled. In this connection the development of the principles governing the flow of alternating currents was undertaken, while at the same time experiments were started on a high-voltage constant-current arc-light circuit, the only supply available.

From the experimental work much was learned, although it was never completed. As at Great Barrington it was learned that a short circuit by a screw-driver could wreck a constant-potential generator; so the lesson was now learned that an open circuit caused by the slipping of a connector from a mercury cup could wreck a constant-current system and cause a general blackout! A mercury-cup contact is ill-suited for high-voltage experiment. Permission for experiment was withdrawn. The work, thus summarily ended, was never renewed; meanwhile a better solution had been found by Elihu Thomson—the "tub" transformer with floating secondary winding delivering constant current from a constant-potential primary winding. Constant-potential primary distribution had become firmly established.

The theoretical work, on the other hand, the development of the principles governing the flow of alternating current, gave better results, opening a fruitful and ever widening field with direct bearing on a-c wave form. This work, prepared initially without thought of publication, formed the basis for the first paper by Bedell and Crehore[5] presented before the Institute at its annual convention (then general meeting) in Chicago just 50 years ago. In this the principles governing current flow in transient as well as in steady conditions were first fully developed. The sequel published in book[6] form later in the same year defined the limitation to telephony (page 201) due to change in wave form of a complex wave along a line[6a] with distributed capacity and the modification produced therein by self-induction, successfully accomplished later by M. I. Pupin with the use of loading coils. In it were included the development of vector methods for solving a-c problems and the extensive use of circle diagrams, now so common.

The first use was here[5,6] made of $j=\sqrt{-1}$ in a-c analysis. Its use in astronomy and other fields had long been known, the symbol $\sqrt{-1}$ as a sign of perpendicularity appearing in a memoir to the Royal Society of Arts and Letters of Denmark by Caspar Wessel[7] in 1797. It is to A. E. Kennelly,[8] however, that credit[9] should be given for bringing out its full significance in the application of complex quantities to a-c problems, and to C. P. Steinmetz for so ably extending its usefulness.

Vector methods and circle diagrams for solving a-c problems were not infrequently criticized, when they were first developed, as being dependent upon the so-called "sine assumption," whereas in fact electromotive forces produced by a-c generators *are not true sine waves.* To this criticism with its part truth there was no categorical answer. As a circle is defined by three points, a circle diagram permits the determination or predetermination of the performance of a-c machines and systems with a minimum amount of observation and calculation. Experimental diagrams[10] were early found to check** closely with theory. The wide use of the circle diagram today is evidence of its value.

The assumption of equivalent sine waves has likewise proved adequate[11] for many cases. For other cases more elaborate methods,[12] sometimes involving vectors in more than two dimensions, have been developed. These methods, also, have been criticized on the ground that electromotive forces produced by most a-c generators *are practically sine waves;* so that these more elaborate methods are unnecessary. Again a part truth! Both assumptions, sine wave and nonsine wave are open to criticism, but each has its field of usefulness. What allowable limits should be set to the departure from a sine wave remains to be determined.

Wave-Form Determination

H. J. Ryan[13] in 1889 made an extensive study of wave forms of a closed-magnetic-circuit transformer under different operating conditions, using a synchronous contact maker to obtain instantaneous readings point by point. In this way complete wave forms of currents and voltages and their phase relations were obtained, showing definitely the behavior of such a transformer. By this method studies were made of generator wave form by Tobey and Walbridge[14] and the wave forms of an open-magnetic-circuit transformer by Bedell, Miller, and Wagner,[15] using a liquid-jet contact maker, with some modifications in methods of measurements and the use of capacitors to improve power factor. The open-magnetic-circuit transformer with its large magnetizing current and low power factor could not survive.

---

* Well into the present century engineers of eminence believed that constant current would be the ultimate in power transmission.

** It is interesting to note that such a close check, even though not complete, could be made without wattmeter, phase, or power-factor meter and with limited instruments for current and voltage measurements. Primary and secondary voltages were determined by reading, with telescope and scale, the elongation of two fine German-silver wires; primary and secondary currents, similarly, with two coarse wires. Very competent were the three observers named in the reference, with later careers of note.

The use of a synchronous commutator[16] in place of a contact maker eliminated error caused by duration of contact and made possible the direct determination of flux. The mechanical plotting of points on a synchronous drum was introduced by E. B. Rosa[17] to eliminate tedious plotting by hand. All methods employing synchronously driven mechanism, however, had their day. Besides being cumbersome and inconvenient, they were limited to commercial frequencies and at best gave only points rather than continuous curves.

Meanwhile, a parallel development, starting with the optical study of the excursions of a telephone diaphragm, had led to the oscillograph,[17a] perfected by Blondel, Duddell, and others, employing a suspended element light enough to follow closely the rapid changes in a quantity under observation. There is no need for expanding on the wide and continued usefulness of the oscillograph in the determination of wave form. Point-by-point methods were thus outmoded. The moving element of an oscillograph, however light it may be, has *some* inertia which, though practically negligible for many purposes, limits its ability to follow very rapid changes in the quantity under observation. A vibrator with no weight at all would be most desirable.

It had long been known that a cathode-ray beam would be deflected by a magnetic or electric field. Ryan[18] grasped at this fact and, with a special cathode-ray tube made for him by Mueller Uri, constructed and used the first cathode-ray oscillograph, an oscillograph in which the moving "part" had no weight and could accurately follow the changes in whatever quantity was under observation. A new field was thus opened.

In the cathode-ray oscillograph the spot of light caused by the cathode ray impinging on a fluorescent screen became a curve, by persistence of vision, when the cathode-ray beam was deflected simultaneously by two fields at right angles, one field being set up by the variable under observation, and the other by a known variable of reference. Various Lissajous figures were thus produced. In studying a-c wave form, Ryan used a known sine wave for reference, the resultant curve being a smooth ellipse in case the wave under observation was also a true sine wave. Departure from an ellipse indicated departure of the unknown a-c wave from a sine wave. The observed curve was then laboriously replotted with time as an axis, as there was then no means for obtaining this directly.

The apparatus used by Ryan was cumbersome. The tube required 5,000–10,000 volts accelerating potential, obtained from a motor-driven Wimshurst machine. Furthermore, it required the maintenance of low vacuum, obtained from elaborate vacuum apparatus with frequent attention. These inconveniences were overcome in the low-voltage hot-cathode tube of Johnson,[19] with an accelerating potential of only 300–500 volts and greater sensitivity. The tube contained a small amount of gas and required no re-evacuation during its life. The general use of the cathode-ray tube for determining wave form thus became possible.

To obtain a linear time axis, as now commonly used, required a saw-toothed wave for the wave of reference, instead of the sine wave used by Ryan. Various means for developing such a wave were advanced, some mechanical, as from a synchronously driven rheostat, and some electrical from various types of circuit. Mechanical means were cumbersome and limited in frequency range and never came into general use. Electrical means for obtaining a saw-toothed wave, properly synchronized and stabilized, led to the oscilloscope[20,21] with linear time axis so widely used today.[22,23] To maintain curves stationary, a stable linear sweep circuit is essential. With the oscilloscope practically no energy is drawn from the circuit under test.

With wave form determined, many methods and machines have been developed for its analysis, when plotted either in rectangular or polar[24] co-ordinates, into its harmonic components. Early attempts were made to determine the separate harmonic components in an a-c wave by direct electrical measurement. Some success was obtained by resonance[25] and by other means, as by passing currents of various harmonic frequency through one coil of a split dynamometer, the current to be analyzed through the other. The results, however, were meager, as the harmonic components without amplification, were too small to give significant measurement. Amplification, however, has made possible the development of many successful analyzers that give the components of an a-c wave directly by electrical measurement.[26–29]

Wave-Form Standardization

With the expanding use of alternating currents, the need for the standardization of wave form arose, and the adoption of some factor or factors that would indicate quantitively the degree of departure from a sine wave. Form factor, the ratio of effective to average value, although useful in connection with transformer loss, had no general significance, widely different wave shapes having the same form factor.[30] Other factors were from time to time proposed for this purpose, including distortion factor, peak factor, harmonic factor, curve factor, and deviation, and in some cases, after discussion, were sanctioned by the standards committee. Each factor had its own[31] significance as the numerical measure of the departure of an irregular wave from a pure sine wave, varying each in its own way with variation of amplitudes, phase and frequencies of the harmonic components of the wave. Each, therefore, had special usefulness for special purposes. Whether a single factor could be found, sufficiently satisfactory for all purposes, was a question.

It was generally agreed that a sine wave of electromotive force at generator terminals or on a transmission line is best for most purposes, and that methods for prescribing allowable departure therefrom should be determined. In 1915 the standards committee, through a subcommittee* on wave form, undertook a study of the subject to ascertain what standard or standards could be specified that would be most suitable in characteristics and practical in application, avoiding tedious analysis and cut-and-try methods as far as possible.

ADMITTANCE STANDARDS

As the troubles caused by a departure from a sine wave depend in many cases upon the frequency of the harmonic or harmonics present, the assignment of penalties to different harmonics according to their frequencies appeared to be an obvious way to make the penalty fit the crime. For doing this, an admittance type of wave-form standard[32] appeared well suited, with the possibility of assigning different allowable weights or penalties to harmonics of different frequencies according to their behavior or misbehavior. The admittance of a circuit is readily measured, being proportional to current. The admittance of a circuit with capacitance (*C*) increases, and in a circuit with inductance (*L*) decreases, with the frequencies of any harmonics in the applied electromotive force. With *L* and *C* both in the circuit, the admittance reaches a resonant peak at a particular frequency, the broadness of resonance being controllable by the resistance (*R*). Values of *L* and *C* can, accordingly, be selected for resonance at a particular resonant frequency, giving maximum penalty to a harmonic of that frequency, the admittance and penalty tapering off on each side, more or less rapidly, according to the value of *R*.

---

* Membership of the subcommittee: F. Bedell, chairman; L. W. Chubb, F. M. Farmer, H. S. Osborne, and L. T. Robinson.

The shape of the tapering slopes on the two sides can be controlled to a certain extent by employing a composite, instead of a simple circuit, with *R*, *L*, and *C* in the admittance standard. Desirable penalties can thus be assigned to different harmonic frequencies according to the degree of crime. The possibility of better weighting thus obtained led to the development of a composite circuit, instead of a simple circuit as an admittance standard, despite the advantage that the latter could be readily duplicated with common laboratory equipment.

The degree of crime, however, and hence the penalty to be assigned, is different in different fields of application, as in power transmission and machinery, in communication or insulation. It soon developed that penalties could not be uniformly prescribed in all fields, and no one universal wave-form standard, however desirable on account of simplicity, would prove generally acceptable. Special standards thus appeared to be necessary to meet practical conditions in each case.

IN INDUCTIVE CO-ORDINATION

The admittance type of wave-form standard was found to be particularly suitable in the inductive co-ordination field. After extensive studies of induction problems involving power and telephone systems, a *telephone interference factor*, TIF, was proposed[33] in 1919 and a TIF meter, of the admittance type, for measuring power-system wave shape in terms of its influence on telephone circuit noise. Definite weightings were determined, based on the interfering effects of different frequencies, depending in part on the telephonic equipment in use and in part on the characteristics of the human ear. In 1935 changes in telephonic equipment and new studies led to new weightings,[34] and the name telephone interference factor was changed to telephone influence factor, as more appropriate. In 1941 weightings[35] were again revised, with corresponding changes in the admittance net work of the TIF meter. Wave-form standards as applied to inductive co-ordination problems are thus being well cared for.

IN THE POWER FIELD

In the power field, deviation and distortion factors are both in use, but not extensively. Deviation is included in some specifications, and acceptance tests are made to see that the specification is met. Distortion factor* is more rarely encountered, being sometimes used by designers for calculating performance of machines. Good wave shape is a matter of evolution, attained by experience. With the increase in size of machines good design for wave form becomes less difficult.

By American Standards Association[36] definition, 1.217–10.95.420:

> "The *deviation factor* of a wave is the ratio of the maximum difference between corresponding ordinates of the wave and of the equivalent sine wave to the maximum ordinate of the equivalent sine wave when the waves are superposed in such a way as to make this maximum difference as small as possible."
>
> "The deviation factor of the open-circuit terminal voltage wave of synchronous machines shall not exceed ten per cent unless otherwise specified." (Rule 3.220)

By ASA definition, 1.218–10.95.430:

> "The *distortion factor* of a voltage wave is the ratio of the effective value of the residue after the elimination of the fundamental to the effective value of the original wave."

To determine deviation, as defined, requires a curve of wave form and the use of cut-and-try methods. Deviation is not directly measurable. In contrast with the admittance type of standard, it takes no account of the frequencies of the harmonic components, a characteristic which may be an advantage or a disadvantage according to the use that is made of it. If there were simple means for its determination, deviation would serve well as a general standard, but no such means are available.

Distortion factor, also, takes no recognition of frequencies, although giving total harmonic content. It can, however, be determined by analytical processes, and indirectly from measurement without requiring a curve of wave form, advantages in its favor.

IN DIELECTRIC TESTS

In dielectric tests, by ASA rule 2.122:

> "The wave shape of the test voltage shall be of acceptable commercial standards: that is, it shall come within the deviation specified as allowable in paragraph 3.220. The test shall be made with a voltage having a crest equal to $\sqrt{2}$ times the test voltage specified."

More recent AIEE Standards[37] specify further in paragraph 3 of appendix:

> "With the test specimen in circuit, the crest factor (ratio of maximum to mean effective) of the test voltage shall not differ by more than five per cent from that of a sinusoidal wave *over the upper half of the voltage range.*"

Crest voltages and crest factor may be determined by the use of a synchronous commutator[38] or rectifying tube, specification for a crest voltmeter being given in paragraph 4-80, AIEE Standards.[37]

In a Standards subcommittee report,[39] the use of crest deviation factor (the departure of the crest factor from 1.414 calculated in percentage) is suggested, and it is recommended that, as deviation requires a trace of wave form, it be not used for specifying wave form in dielectric power-factor measurements, and that distortion factor be adopted in its stead.

Summary

Since 1890, when alternating current was considered just plain "freak," not obeying Ohm's law and "clogging itself in its circuits," our knowledge (with or without calculus) has become greatly advanced and the importance of wave form, "whatever that may be," so well recognized as to require most careful standardization.

A fuller discussion of wave-form standardization would here be out of place. Standards must always be subject to revision with technological development, such revision being made only after a strong need has developed. This is not the occasion to suggest revision.

The author has outlined the history of wave form, without venturing a prediction as to the future. The paper merely runs a thread through a maze of material with no pretention to completeness, the story being more fully told in the references and their extensive bibliographies.

References

1. ALTERNATING CURRENT VERSUS DIRECT CURRENT, L. B. Stillwell. ELECTRICAL ENGINEERING, volume 53, May 1934, pages 708–11.

2. ALTERNATING CURRENT IN AMERICA, William Stanley. *Journal* of the Franklin Institute, volume 173, 1912, pages 561–80.

3. HISTORY OF THE TRANSFORMER (book, translated from the German), F. Uppenborn. E. and F. N. Spon, 1889.

4. DYNAMOELECTRIC MACHINERY (book), S. P. Thompson. E. and F. N. Spon, 1892. Fourth edition, page 727.

---

* Distortion factor is frequently used in the communication field in rating high-quality program and broadcasting equipment.

5. Derivation and Discussion of the General Solution for the Current Flowing in a Circuit Containing Resistance Self-Induction and Capacity With Any Impressed Electromotive Force, F. Bedell, A. C. Crehore. AIEE Transactions, volume 9, 1892, pages 303–74.

6. Alternating Currents: an Analytical and Graphical Treatment for Students and Engineers (book), F. Bedell, A. C. Crehore. W. J. Johnston Company, 1892, pages 1–325.

6a. Electrical Papers (book), O. Heaviside. Electrician Printing and Publishing Company, 1892.

7. Revolving Vectors (book), G. W. Patterson. The Macmillan Company, 1911. Page 1.

8. Impedance, A. E. Kennelly. AIEE Transactions, volume 10, 1893, pages 175–216.

9. Letter to the editor, E. W. Rice, Jr. *General Electric Review*, volume 27, 1924, page 132.

10. Transformer Diagrams Experimentally Determined, F. Bedell, assisted by A. W. Berresford, W. M. Craft, B. Gherardi, Jr. *Proceedings* International Electrical Congress in Chicago (book), August 1893. AIEE, 1894. Pages 234–51.

11. Reactance, C. P. Steinmetz, F. Bedell. AIEE Transactions, volume 11, 1894, pages 640–8.

12. Nonharmonic Alternating Currents (with 51 references), F. Bedell. AIEE Transactions, volume 46, 1927, pages 648–58.

13. Transformers, H. J. Ryan. AIEE Transactions, volume 7, 1889, pages 1–19.

14. Investigation of Stanley A-C Arc Dynamo, W. B. Tobey, G. H. Walbridge. AIEE Transactions, volume 7, 1890, pages 367–78.

15. Hedgehog Transformer and Condensers, F. Bedell, K. B. Miller, G. F. Wagner. AIEE Transactions, volume 10, 1893, pages 497–527.

16. The Use of the Synchronous Commutator in A-C Measurements, F. Bedell. *Journal* of the Franklin Institute, volume 176, 1913, pages 385–404.

17. An Electric Curve Tracer, E. B. Rosa. *Physical Review*, volume 6, 1898, pages 17–42.

17a. Oscillographs and Their Tests, A. E. Kennelly, R. N. Hunter, A. A. Prior (with extensive bibliography). AIEE Transactions, volume 39, 1920, pages 443–87.

18. The Cathode-Ray A-C Wave Indicator, H. J. Ryan. AIEE Transactions, volume 22, 1904, pages 539–48.

19. A Low-Voltage Cathode-Ray, Oscillograph, J. B. Johnson. *Journal* of the Optical Society of America, volume 6, 1922, pages 701–12.

20. The Oscilloscope: a Stabilized Cathode-Ray Oscillograph With Linear Time Axis, F. Bedell, H. J. Reich. AIEE Transactions, volume 46, 1927, pages 546–9.

21. Stabilized Oscilloscope With Amplified Stabilization, F. Bedell, J. G. Kuhn. *Review of Scientific Instruments*, volume 1, 1930, pages 227–36.

22. The Cathode-Ray Oscillograph, J. B. Johnson. *Journal* of the Franklin Institute, volume 212, 1931, pages 687–715.

23. Theory and Applications of Electron Tubes (book), H. J. Reich. McGraw-Hill Book Company, Inc., New York, N. Y., 1939, page 592.

24. Analysis of Periodic Waves, L. W. Chubb. *Electric Journal*, volume 11, 1914, pages 91–6.

25. Resonance Analysis of Alternating and Polyphase Currents, M. I. Pupin. AIEE Transactions, volume 11, 1894, pages 523–50.

26. An Electrical Frequency Analyzer, R. L. Wegel, C. R. Moore. AIEE Transactions, volume 43, 1924, pages 457–65.

27. The Empirical Analysis of Complex Electric Waves, J. W. Horton. AIEE Transactions, volume 46, 1927, pages 535–40.

28. An Analyzer for the Voice Frequency Range, C. R. Moore, A. S. Curtis. *Bell System Technical Journal*, volume 6, 1927, pages 217–29.

29. Analyzer for Complex Electric Waves, A. G. Landeen. *Bell System Technical Journal*, volume 6, 1927, pages 230–47.

30. Form Factor and Its Significance, F. Bedell, assisted by R. Bown, H. A. Pidgeon. AIEE Transactions, volume 34, 1915, pages 1135–42.

31. Distortion Factors, F. Bedell, assisted by R. Bown, C. L. Swisher. AIEE Transactions, volume 34, 1915, pages 1143–57; discussion, pages 1171–2000.

32. Characteristics of Admittance Type of Wave-Form Standard (a review of work of subcommittee on wave-form standard of the standards committee), F. Bedell. AIEE Transactions, volume 35, 1916, pages 1155–70; discussion 1171–86.

33. Review of Work of the Subcommittee on Wave-Shape Standard of the Standards Committee, H. S. Osborne. AIEE Transactions, volume 38, part I, 1919, pages 261–88; discussion, pages 291–303.

34. Measurement of Telephone Noise and Power Shape, J. M. Barstow, P. W. Blye, H. E. Kent. AIEE Transactions, volume 54, 1935, December section, pages 1307–15.

35. Frequency Weighting for Message Circuit Noise, engineering report 45. Joint Committee on Development and Research, Edison Electric Institute and Bell Telephone System, July 24, 1941, pages 1–12.

36. American Standards for Electrical Rotating Machinery, electrical standards committee. American Standards Association, 1936.

37. AIEE Standard No. 4, Measurement of Test Voltage in Dielectric Tests (revised January 1940, with extensive bibliography). June 1940.

38. Notes on the Synchronous Commutator, J. B. Whitehead. AIEE Transactions, volume 29, 1920, pages 407–38.

39. Report on Wave Form in Dielectric Power-Factor Measurements, standards subcommittee report. Electrical Engineering, volume 59, June 1940, pages 255–6.

Design and Operation of High-Voltage Axial Air-Blast Circuit Breakers

ARMIN K. LEUTHOLD
MEMBER AIEE

Synopsis: The air-blast high-voltage circuit breaker has been developed and applied on high-voltage systems of 150 and 240 kv. Both single-phase and three-phase automatic high-speed reclosing can be carried out with this new type of circuit breaker. The principles of design and operation are explained and the construction of two high-voltage air-blast circuit breakers rated at 150 kv and 220 kv, installed in Canadian power-distribution plants, is described and supplemented by a discussion of performance tests and oscillograms. The construction of an air-blast circuit breaker with axial blast, designed for extremely high voltage and high interrupting capacity, is explained, and results of various tests discussed.

THE air-blast type of circuit breaker was developed very rapidly from the indoor low-voltage type to the outdoor high-voltage type of circuit breaker, as extensive test experience clearly showed the many distinct advantages of applying compressed air for arc interruption. Besides the elimination of the oil hazard, the application of compressed air assures a very fast arc-interrupting ability and enables extremely fast tripping and automatic high-speed reclosing, by means of which the ideal protection of high-voltage power-transmission lines can be achieved.

The description of the following high-voltage air-blast circuit-breaker installations will show to what extent this type of breaker has been developed.

CONSTRUCTION AND OPERATION OF 150-KV AIR-BLAST CIRCUIT BREAKER

The construction of the 150-kv air-blast breaker is illustrated in Figure 1. The breaker consists of three individual poles which are pneumatically interconnected or controlled by a common closing and tripping shaft. Each pole comprises a compressed-air tank (1), with a control valve and piston assembly (2, 3, 4), and a porcelain insulator column (5), on which is mounted the arcing chamber (6), which contains two arc breaks (7), and an air-saving piston arrangement (8). A movable isolating contact (9) is connected in series with the arcing contacts and is operated by a rotating insulator (10). The stationary isolating contact (11) is supported on an insulator (12) which can be designed to accommodate, at the same time, the current transformers for the necessary protective relays and instruments.

To obtain a maximum arc-interrupting effect, an optimum distance between the arcing contacts is required and it is, therefore, necessary to connect a movable isolating contact in series with the arcing contacts in order to provide a satisfactory isolating distance between the live breaker parts in the open breaker position. This separation of the arc-interrupting and isolating functions allows the arcing contacts to be designed relatively small and light, and this, in turn, makes it possible to obtain a very fast opening as well as a high-speed reclosing of the breaker by means of operating the arcing contacts only.

In cases where automatic high-speed reclosing operation is required, a separate insulator tube (13) is applied to supply compressed air for the automatic high-speed reclosing of the arcing contacts.

BREAKER OPERATION

In the open position of the breaker, the isolating contact (9) is open while the arcing contacts (7) are held closed by spring pressure.

When the breaker is closing an auxiliary control valve becomes energized and feeds air from the breaker tank to the isolating contact piston (4), which closes the movable isolating contact (9) by means of a bevel gear drive and the rotating insulator (10).

When the breaker is tripped, compressed air is fed through an auxiliary control valve from the breaker tank to an auxiliary piston (3) in order to open the main blast valve (2), by means of which air flows with high velocity through the insulator column (5) into the arcing chamber (6) and opens the arcing contacts (7) pneumatically and extinguishes the arc at an early current zero. After the arc is extinguished, the air-saving piston (8) closes, and full air pressure is maintained between the open arcing contacts to provide a high dielectric medium which prevents reignition of the arc during the time when the movable isolating contact (9) opens. The pneumatic control is so arranged that, as soon as the isolating contact is fully open, the main air-blast valve closes again, and only the arcing contacts move back into closed position because of spring pressure.

In case automatic high-speed reclosure is applied, the arcing contacts open as on a normal tripping operation, but without operating the movable isolating contact (9), and they are held open under air pressure until, by means of an auxiliary air supply through the reclosing insulator tube (13), these contacts reclose again.

In case of a permanent fault, the breaker immediately opens again after the first automatic reclosure, in a similar way as on a normal three-phase tripping, by the opening of the arcing and isolating contacts, and will remain in open position.

150-Kv Air-Blast Circuit-Breaker Installation in Canadian Power Plant

Figure 2 shows the air-blast breaker installation of the Aluminum Company at Arvida. This breaker, designed for a service voltage of 150 kv and an interrupting capacity of 1,800,000 kva, has been in service since 1939. The large insulators in the foreground, upon which the stationary isolating contacts are mounted, also contain the current transformers which are necessary for the protective relays; otherwise the construction is the same as described previously. Automatic reclosing operation was not requested, and, therefore, this breaker was not equipped with reclosing device.

ARCING CHAMBER

The construction of the arcing chamber of this breaker is illustrated in Figure 3. Two arc breaks are connected in series which operate as follows:

When the main air-blast valve has opened, compressed air flows under high velocity through the hollow breaker insulator column and ducts into the outside space of the lower and upper arcing chambers, by which means the arcing contacts (1) move their full stroke away from the stationary arcing contacts (2), thereby striking the arcs. Compressed air rushes through the hollow movable arcing contacts and through the muffler (3) to the open air and envelopes the arcs by a stream of high-velocity air which extends the arcs while, by forceful cooling due to air expansion, the arc paths become deionized to such an extent that the arcs extinguish at an early current zero.

Paper 42-155, recommended by the AIEE committees on power transmission and distribution and protective devices for presentation at the AIEE Pacific Coast convention, Vancouver, British Columbia, Canada, September 9–11, 1942. Manuscript submitted May 19, 1942; made available for printing July 30, 1942.

ARMIN K. LEUTHOLD is engineer with the Brown Boveri and Swiss Electric Company of Canada, Ltd., Montreal, Quebec, Canada.

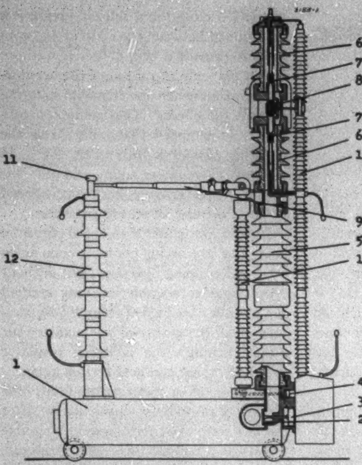


Figure 1 (left). Construction of 150-kv air-blast circuit breaker with double arc break

Figure 2 (right). 150-kv air-blast circuit-breaker installation of the Aluminum Company of Canada, Ltd.

When the arcing contacts move into the open position, some ports (4) become uncovered, through which compressed air can flow to the underside of the air-saving pistons (5), and, therefore, these pistons move against the arcing chamber partition, thus preventing a further escape of compressed air from the arcing chamber to atmosphere. A pneumatic time lag retards the closing of the air-saving pistons long enough to ensure positive extinction of the arcs. At the same time, when compressed air is fed into the arcing chamber, it is also admitted to an auxiliary piston which opens, with a time delay of a few cycles, the movable isolating contact. After the latter is fully open the main blast valve closes, and the air-saving piston returns to open position.

OPERATION

The operation of this type of air-blast circuit breaker is illustrated in oscillogram Figure 4, where a make-break operation is recorded. On a single-phase short-circuit at a voltage of 142 kv, a current equal to a three-phase interrupting capacity of 638 megavolt amperes was interrupted. As indicated on this oscillogram, the short-circuit was initiated at (1), at (2) the breaker tripping coil was energized, at (3) the arcing contacts were separated, and at (4) the arc interruption was completed. Including the arcing time of 0.007 second, the short-circuit interrupting time of the breaker proper (protection relay time excluded) amounted to 0.046 second.

The first high-voltage-type air-blast breaker in Canada was installed at the power distribution plant at Arvida in 1939 and has operated up to the present time very satisfactorily without any failure. A second 150-kv air-blast circuit breaker was installed at the same power distribution plant in 1941. This second breaker has also given excellent service. Experience has shown that no appreciable arcing-contact deterioration is caused even after several short-circuit interruptions. The inspection of the arcing contacts can be carried out in a few minutes, and the maintenance work on these breakers is very little compared to that on an oil circuit breaker.

150-KV AIR-BLAST CIRCUIT BREAKER WITH AUTOMATIC RECLOSING DEVICE

Figure 5 illustrates the same type of air-blast breaker rated 150 kv but equipped with additional devices for automatic reclosing. Several breakers of this type are installed outdoors in European power distribution plants and have given very successful operating results on applying automatic high-speed single and three-phase reclosing.

Oscillogram Figure 6 illustrates a short-circuit interruption with high-speed reclosing which was carried out with this type of breaker on an overhead line of 150 kv. On the upper oscillogram the interruption of an arcing short circuit is shown with automatic reclosing. Experience has shown that restriking of transient system faults could be prevented in most cases if the breaker stayed open approximately 10 to 15 cycles. Therefore, the breaker reclosing time was set in such a way that the breaker was delayed in reclosing approximately 0.19 second. With this time delay in regard to the automatic reclosing, very satisfactory operating results were obtained. At a voltage of 150 kv, a current of 2,200 amperes was interrupted. The total short-circuit interrupting time of the breaker proper (protective relay time excluded) was 0.043 second.

The lower oscillogram of Figure 6 shows the interruption of a metallic permanent short circuit. After the first arc interruption is finished at (2), the breaker recloses automatically only once at (3), and then immediately opens a second time by a final trip and stays open.

BREAKER OPERATION AT LOW TEMPERATURE AND UNDER HEAVY ICE FORMATION

In order to check that the movable breaker parts and piston mechanism operate reliably even under heavy snow and

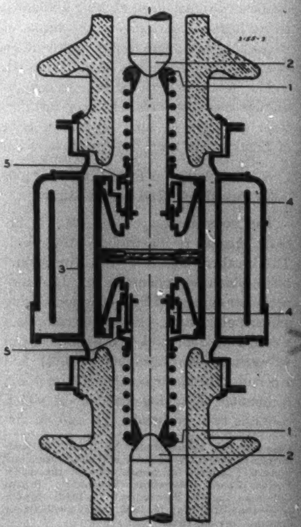


Figure 3. Arcing chamber of 150-kv air-blast circuit breaker

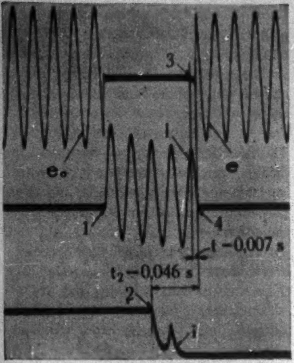


Figure 4. Make-break operation on a single-phase short-circuit by 150-kv air-blast circuit breaker

e_o—Line voltage (142 kv)
e —Recovery voltage (130 kv)
I —Fault current (2,450 rms amperes)
i —Tripping-coil current

ice formation, extensive tests under similar conditions as shown in Figure 7 have been carried out with various high-voltage air-blast breakers, and it was found that, even under extremely heavy snow and ice formation, all the movable breaker parts operated very satisfactorily, both as to tripping and closing of the breaker.

In order to prevent condensation and ice formation inside the outdoor air-pipe system and breaker parts, special air-drying equipment was applied through which the compressed air is filtered and dried. Furthermore, an additional outdoor-erected air-storage tank was used in order to refill the breaker air tank, at each breaker operation, with air at approximately the same temperature as the breaker parts.

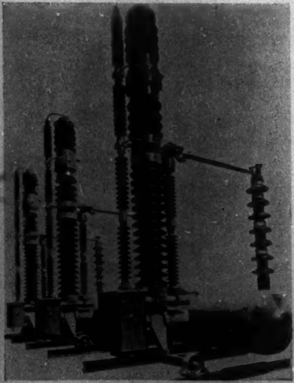


Figure 5. 150-kv outdoor air-blast circuit breaker with automatic high-speed reclosing devices

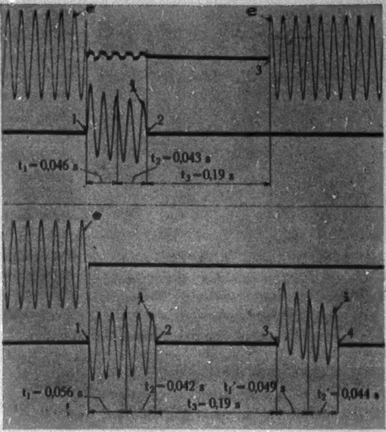


Figure 6. Short-circuit interruption with automatic reclosing by 150-kv air-blast circuit breaker

e—Line voltage (150 kv)
i—Fault current (2,200 rms amperes)

220-Kv Air-Blast Circuit Breaker Installed Outdoors on a Canadian High-Voltage Power Line

Figure 8 shows a 220-kv air-blast high-speed circuit breaker installed in the outdoor switching station of the La Tuque power plant of the Shawinigan Water and Power Company. This breaker is rated for an arc-interrupting capacity of 2,500,000 kva at 220 kv and is equipped with additional devices for carrying out single-phase or three-phase automatic high-speed reclosing.

CONSTRUCTION

Each breaker phase has a separate undercarriage upon which two large insulator columns are mounted; to each breaker undercarriage are attached four air receivers and a complete control valve and piston assembly as required for the operation of each individual breaker pole. Each insulator column consists of a large foot insulator upon which a double arcing-contact chamber is mounted, thus forming four arc breaks in series for each breaker phase. Inside the foot insulator are located two porcelain pressure tubes for supplying compressed air for the arc interruption and for the high-speed reclosing. Furthermore, to each insulator column is attached a movable isolating arm which is operated by means of rotating insulator and bevel gear drive from a separate piston on closing and normal tripping. Otherwise, the design of each insulator column is similar to that described on the 150-kv air-blast circuit breaker.

ARCING CHAMBER

The arcing chamber, mounted on top of the foot insulator, is illustrated in Figure 9. Two movable arcing contacts (1) and two stationary contacts (2) are mounted inside two porcelain insulators (3), which are surrounded by capacitance tubes (6) which control the potential

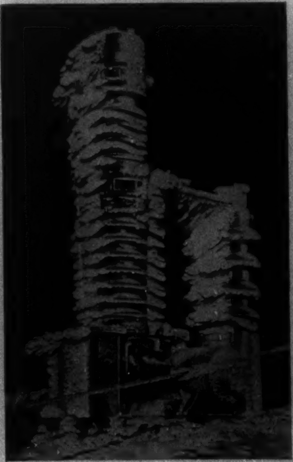


Figure 7. Air-blast circuit-breaker tests under heavy snow and ice formation

Figure 8. 220-kv air-blast circuit-breaker installation of The Shawinigan Water and Power Company

distribution between the individual arc breaks. The movable arcing contacts (1) are operated by means of a differential piston (7) and a lever arrangement (5); the latter assures simultaneous opening of the arcing contacts in series. In the intermediate piece (4) and also in the cast-iron piece (8) located at the top and at the bottom of the fixed arcing contacts, are the air-saving pistons (9), through which the compressed air in the arcing chamber escapes to atmosphere.

OPERATION

When the breaker is tripped by the opening of the main blast valve, compressed air is supplied through the large porcelain pressure tube (10) and ducts into the outside space of the lower and the upper arcing chambers and also through a duct into the intermediate piece (4) behind the differential piston (7). As soon as the air pressure is built up, this differential piston opens the arcing contacts by compressing the contact spring (11), and a stream of air flows with high velocity through the hollow arcing contacts and the air-saving pistons to atmosphere, which creates a rapid deionization of the arcing path and extinguishes the arc at next zero passing. At the same time part of the compressed air also flows through a special duct into the air-filling volume (12), which closes the air-saving pistons with a certain time delay, thus preventing a further escape of compressed air to the outside after the arc is extinguished. Closing the air-saving pistons, the compressed air re-establishes a high dielectric strength between the arcing contacts and, therefore, prevents reignition by the recovery voltage. At the same time, when compressed air is supplied to the arcing chambers, air is admitted also to the isolating contact piston which opens, with a short time delay, the movable isolating contact under no-load condition. After the complete opening of the isolating contacts, the main blast valve closes again, the arcing contacts close, and the air-saving pistons open again by spring pressure.

On tripping with automatic high-speed reclosing, only the arcing contacts open as previously described—but without opening the movable isolating contact. The arcing contacts are held open under air pressure until, by means of an auxiliary reclosing piston, air is admitted through the porcelain pressure tubes (13, 14) to the differential piston (7) and air-saving pistons (9), which simultaneously reclose the arcing contacts and open the air-saving pistons.

The following operations are carried out by the breaker protective and control devices:

By means of control relays, the breaker opens at the first tripping impulse, either single or three-phase, by the arcing contacts only, and recloses automatically upon an auxiliary reclosing relay impulse with a definite time-delay which can be adjusted to any value most suitable for the particular network. In case of a single-phase tripping by a permanent single-phase short-circuit, the breaker automatically recloses only once on the faulty phase, and immediately opens again by a final three-phase trip with the arcing and isolating contacts and stays open.

By means of a control and minimum air-pressure valve, it is assured that the breaker operates only if the available pressure of the compressed air, stored in the breaker tanks, is in the range of the admissible operation pressure. If on any reclosing the air pressure drops lower than normal, the reclosing function will not be carried through, and the breaker opens immediately by a three-phase final tripping, which will be initiated by means of a contact pressure gauge.

If while closing the breaker, an urgent trip is required, the air to the closing piston will be shut off immediately by means of an anti-pumping valve, which allows the tripping operation to be carried out without delay. By these various protective and control devices, it is assured that the breaker will always carry out the best suitable breaker function in connection with the particular operating condition.

TEST RESULTS

Field tests have been carried out with this installation where the principle functions were recorded simultaneously on all three breaker poles. Furthermore, by high-voltage short-circuit tests, the arc interrupting ability of the breaker was checked.

The upper oscillogram of Figure 10 illustrates the operation of this breaker in case of a permanent single-phase fault. Only the faulted phase opens initially, followed by automatic reclosure after a short time delay. Thereupon, the breaker immediately opens again, this time on all three phases, both arcing and isolating contacts, and remains opened. As shown on the lower oscillogram of Figure 10, a similar sequence is carried out for a permanent three-phase fault, except that in this case all three phases are opened on the first as well as on the second opening. The recorded potential of the travel indicator, which was attached to the rotating insulator of the movable isolating contact, shows that at the first interruption the movable isolating contact did not move, while at the second, final interruption this contact opened a few cycles after the arcing contacts parted.

From these oscillograms, Figure 10, the following breaker speeds were obtained: The breaker opening time until the arcing contacts part is $2^{3}/_{4}$ to 3 cycles. The automatic reclosing is delayed 23 cycles on single-phase and 16 cycles on three-phase circuit interruption, but this delay can be set to any desired value by means of a relay with an adjustable time-delay device. The movable isolating contact parts approximately four cycles after the opening of the arcing contacts.

For testing the arc-interrupting ability of this breaker, special short-circuit tests were carried out after the installation of this breaker was completed. At first, sev-

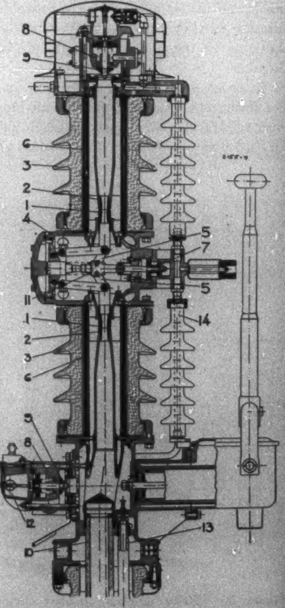


Figure 9. Arcing chamber of 220-kv air-blast circuit breaker

eral short-circuit tests (single-phase-to-ground) were carried out during which the line voltage was gradually raised from 220 kv to 260 kv. A fault current of approximately 280 to 330 amperes was interrupted without any difficulty, with an arcing time of less than one cycle.

In addition, similar short-circuit tests were carried out on the network with the power plants connected in parallel so as to obtain the maximum possible short-circuit capacity. With all these networks in parallel, the breaker interrupted a short circuit which was equal to a three-phase interrupting capacity of approximately 1,000,000 kva as illustrated in oscillogram Figure 11. The time required for the protective relay and the auxiliary contacts of the control circuit of the particular test arrangement was $3^{1}/_{2}$ cycles, the breaker opening time proper, including arcing time, was only $4^{1}/_{2}$ cycles, and the arcing time was less than one cycle.

Tests also were carried out with all networks in parallel where the breaker was switched twice into a short circuit by automatic high-speed reclosing with a time delay of only 17 cycles between the first and the second arc interruption, and twice a short-circuit current of 2,740/2,330 amperes at approximately 220 kv was interrupted satisfactorily with an arcing time at both interruptions of approximately one-half cycle. At the first interruption (which was equal to a three-phase interrupting capacity of approximately 1,000,000 kva) the arcing contacts opened without opening of the isolating contacts and, therefore, the full recovery voltage of approximately 220 kv had to be held between the small distance of the four arc breaks in series until the breaker automatically reclosed again.

All these tests, including the test with automatic reclosing, had so little influence on the large network system that these short circuits could hardly be noticed at the different power stations. Full stability of the networks was maintained through all these various short-circuit tests.

After several weeks of satisfactory operation with this breaker, a flashover took place inside the small porcelain pressure tube for automatic reclosing, and this put the breaker out of operation until the replacement of the damaged parts. It is assumed that probably condensation on the surface of this porcelain tube led to this flashover.

From the various tests and operating results we may conclude that these high-voltage air-blast breakers of 150 and 220 kv have shown the advantage of interrupting large short-circuit capacities with an air stream of high velocity and the facility of carrying out automatic high-speed single and three-phase reclosing.

Latest Design of an Air-Blast Circuit Breaker for Extremely High Voltage and High Interrupting Capacity

Based on experience from the high-voltage air-blast circuit breakers installed in various power plants and on extensive high-voltage arc-interrupting tests carried out in their large breaker test room,

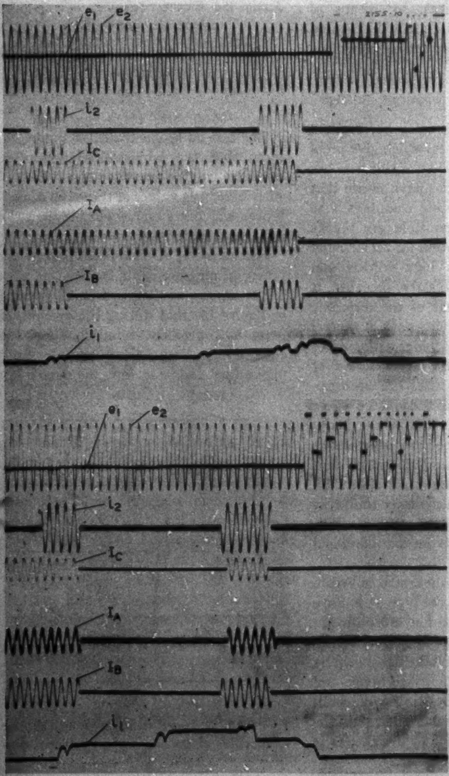
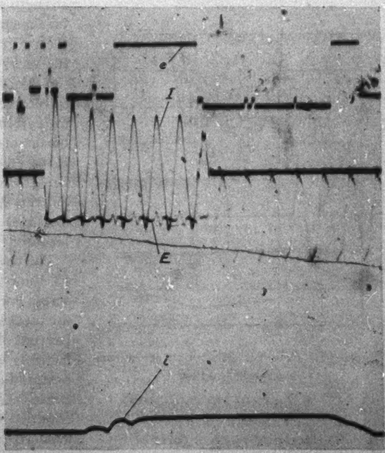


Figure 10 (left). Automatic single-phase and three-phase reclosing with final three-phase tripping by 220-kv air-blast breaker

i_1—Breaker control impulses
I_B, I_A, I_C—Current of breaker phases
i_2—Tripping relay impulse
e_1—Travel indicator potential
e_2—Timing wave (60 cycles)

Figure 11 (below). Interruption of a single-phase short-circuit to ground at 220 kv by air-blast breaker (interruption equal to three-phase interrupting capacity of 1,000,000 kva)

i—Breaker control impulses
E—Compensated voltage across fault
I—Fault current phase B
e—Travel indicator potential



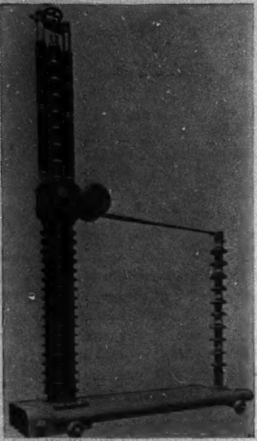


Figure 12. Air-blast circuit breaker for extremely high voltage and high interrupting capacity with eight arc breaks in series

Brown, Boveri and Company have developed, in addition to the previously described air-blast breakers of 150 and 220 kv, an air-blast breaker for extremely high voltage and high interrupting capacity as illustrated in Figure 12.

CONSTRUCTION

On this breaker eight potential-controlled arc breaks are connected in series, which open simultaneously by means of compressed air, and which provide an extremely short arc interruption. In regard to the various pistons and other breaker parts, the design in principle is similar to the previously mentioned design but simplified to a large extent. The inspection of the individual arcing contacts can be carried out in a few minutes, since it only requires loosening the spindle of a press clamp for taking off the contacts for inspection.

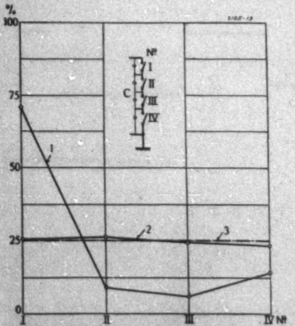


Figure 13. Potential distribution across four arc breaks in series

1. Without potential control
2. With potential control
3. Ideal distribution of potential

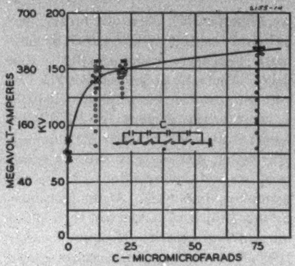


Figure 14. Increase in interrupting capacity and interrupting voltage in function of the control capacitance (C) across the four arc breaks in series

This breaker is also equipped with high-speed reclosing devices, by means of which single and three-phase high-speed reclosing can be carried out as previously described.

BREAKER TESTS

The design of this breaker has resulted from the following tests, which give proof of the interrupting ability of this breaker. Extensive investigations have shown that the application of arcing chambers with potential-controlled multiple arc breaks presents the best solution for an air-blast breaker of extremely high voltage and high interrupting capacity, because it allows re-establishment of the dielectric strength across the arcing path at several breaks simultaneously, which extinguishes the arc very effectively. With the rise of the recovery voltage after arc interruption, the distribution of the voltage across the arc breaks is influenced by the charging currents of the capacitances across the breaks and to earth and, therefore, it is of paramount importance to assure an equal distribution of the voltage across the different arc breaks by means of connecting small capacities across the individual breaks.

The test results, as illustrated in Figure 13, show clearly that the highest voltage across one break (with four uncontrolled arc breaks in series) can rise as high as 71 per cent of the total voltage (as shown by curve 1) while, if all four arc breaks are potential-controlled by means of small capacitances, very equal potential distribution between the four arc breaks is attained (as shown by curve 2).

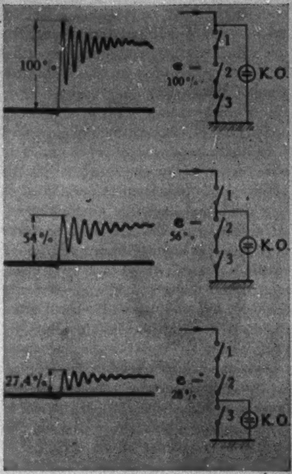


Figure 15. Oscillations of the recovery voltage across potential-controlled arc breaks during the breaker opening, recorded by cathode-ray oscillograph

e—Distribution of the potential during the oscillation
KO—Cathode-ray oscillograph

The test data, illustrated in Figure 14, show that the admissible interrupting capacity can be considerably increased with increasing values of the small capacitances which are connected between the individual arc breaks.

Furthermore, it has been proved by potential measurements carried out with a cathode-ray oscillograph (as illustrated in Figure 15) that during the opening of potential-controlled arcing contacts, the equal distribution of the potential is fully maintained and, therefore, the stresses on

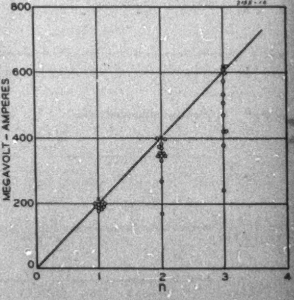


Figure 16. Increase in interrupting capacity in function of the number of potential-controlled arc breaks

Method for A-C Network Analysis Using Resistance Networks

WALDO E. ENNS
ASSOCIATE AIEE

THE mathematical solution of a-c network problems, of the type known as load distribution and voltage studies, is difficult and tedious for complex networks.

Where available, the a-c network analyzer or calculator affords the means of solving this type of problem, as well as many other problems, with good speed and accuracy.

It is the purpose of this paper to describe a new method of calculating a-c network problems and to illustrate with a practical example how a conventional d-c board, comprised of resistance circuit elements, may be used to solve load distribution and voltage studies. The writer has developed a new type of d-c board which would facilitate the use of the method described in this paper, but for the purpose of the present paper the application is limited to the existing types of d-c boards.

The scope of the present paper is restricted to the analysis of three-phase networks with balanced loading on the three phases. If the application to load distribution studies is understood, it is believed the use of the method for short-circuit studies will be evident. For short-circuit work this method can be used to advantage if the resistance of the network circuits is appreciable, so that a network setup representing reactance does not give sufficiently accurate results, and where a setup representing impedance is inaccurate because of differences in ratio of X to R for the circuits forming the network.

In contrast with the a-c network analyzer, inherently this method is not exact, but by successive approximations an exact solution may be obtained. Fortunately the first approximation is close enough for most practical problems, and rarely is it necessary to go further than the second approximation to obtain the desired accuracy. As the illustration shows, the method is simple and is quicker than other methods in analyzing complex networks with the exception of the a-c analyzer. Therefore the writer believes that the method of this paper as applied to a d-c board can be a useful tool for engineers in system planning work.

Simple computations are involved to obtain the voltage drop in a radial circuit with known line constants and loading, particularly when the circuit may be treated as a lumped impedance.

When a current with inphase component $(+I_p)$ and quadrature component $(+I_q)$ flows through a circuit of resistance R and reactance X, the inphase component of voltage drop is $V_p=I_pR-I_qX$, and the quadrature component is $V_q=I_qR+I_pX$. The components of current and voltage drop refer to the same reference axis.

If a current equal to I_p is passed through a resistance equal to R, the voltage drop I_pR can be measured. If a current equal to I_q is passed through a resistance equal to X, the voltage drop I_qX can be measured. Likewise passing I_p through X and I_q through R, I_pX and I_qR can be measured, and, by using the equation for V_p and V_q, the values of the components of voltage drop are obtained. These simple equations are the basis for the method of this paper.

When these equations are applied to network analysis, it is necessary to choose a reference voltage at one point in the network to which all voltage and current components refer for phase position. For simplicity, a network with balanced loading, is analyzed on a single-phase-to-neutral basis. To obtain a correct solution, it is necessary to satisfy Kirchhoff's laws for a-c network, which may be expressed as follows:

(*a*). The algebraic sum of the components of current toward any junction point is zero.

(*b*). The algebraic sum of the components of voltage around any closed path in the network is zero.

Paper 42-149, recommended by the AIEE committee on power transmission and distribution for presentation at the AIEE Pacific Coast convention, Vancouver, British Columbia, Canada, September 9–11, 1942. Manuscript submitted June 1, 1942; made available for printing July 17, 1942.

WALDO E. ENNS is electrical engineer with Portland General Electric Company, Portland, Oreg.

---

each individual arc break are in proportional relation to the total interrupting capacity.

As a result of these tests, a definite conclusion can be drawn regarding the performance of the breaker under its rated interrupting capacity by applying the proportional interrupting capacity on one break only. This contention has been proved by the test data shown in Figure 16 where it is shown that the maximum admissible interrupting capacity increases practically in proportion to the number of potential-controlled arc breaks. For high-voltage circuit breakers, relatively high interrupting capacities are required which far exceed the maximum short-circuit capacity available at the present test plants. It is, therefore, of great importance to note from these tests that a breaker with potential-controlled multiple arc breaks can be tested accurately by interrupting the partial load with one break only, which enables one to judge what the performance of this breaker would be under full rated interrupting capacity.

Therefore, judging from these test results, it should be possible today to design, for example, an air-blast circuit breaker for a voltage as high as 500 kv without requiring a test plant of such a high voltage.

Conclusion

The construction of the various high-voltage air-blast breakers and the operating and test data show that today this type of breaker is developed to such an extent that very valuable operating results can be obtained. In summarizing, we can conclude that, besides lowering the maintenance cost, the stability of the transmission systems can further be increased because of the breaker interrupting speed and the possibility of carrying out high-speed reclosing.

References

1. Circuit Breakers for Very High Service Voltage, H. Thommen. *Brown Boveri Review* (Switzerland), June 1941.

2. Ultrahigh-Speed Reclosing of High-Voltage Transmission Lines, Philip Sporn, D. C. Prince. AIEE Transactions, volume 56, 1937, January section, pages 81–90, 100.

3. Récherches sur le Ré-enclenchement Rapide, en Cas de Courts-Circuits sur les Réseaux Aériens, et sa Réalisation par l'Emploi du Disjoncteur Pneumatique Ultra-rapide, H. Thommen. International Conference on Large High-Voltage Systems (CIGRE), 1939.

4. Field Tests and Performances of a High-Speed 138-Kv Air-Blast Circuit Breaker, Philip Sporn, H. E. Strang. AIEE Transactions, volume 61, 1942, January section, pages 1–6.

In the practical network problem which follows, the compliance with Kirchhoff's laws is illustrated; however, it should be noted that in this particular problem, the transformers involved have the same ratio, so that there is no boost or buck in the voltage in any closed path in the network. The method of treating boost or buck in a closed path will be discussed later in the paper.

Application of Method to Network Problem

Following is an outline of procedure for making a load distribution and voltage study for a complex network with the aid of a conventional d-c calculating board and illustrated with a practical problem. The network shown on Figure 1 consists of 116 miles of 115-kv and 395 miles of 57-kv three-phase transmission circuits. The actual systems involved have more circuit mileage which is radial with respect to the network shown and so is not included in the problem. The estimated loads and generation for the December 1942 peak are used in the study with a total megawatt input in excess of 200 megawatts; however due to simplification by subtracting local load from generation, the net total input is reduced to 157 megawatts for the purpose of the network problem.

1. One-Line Diagram of Network

Figure 1 is the one-line diagram of the network simplified somewhat from the actual system to eliminate radial lines which are set up as loads at the network busses.

The method of lettering busses and numbering circuits as shown on Figure 1 is a satisfactory means of simplifying the recording of data and lessening the chance of making errors in the direction of current flow or voltage drop in the network circuits.

2. Circuit Constants

The circuit constants R, X, B, and G are required as shown in Table I. The constants are on a common phase voltage base of 57 kv in the problem but may be on any suitable common voltage or kilovolt-ampere base, if the currents are on the same base. (Where parallel circuits interconnect busses, the constants of the equivalent circuit will simplify the board setup, and if the parallel circuits have ratios of X/R which are substantially different, it is essential that the equivalent circuit be used, otherwise additional successive approximations may be necessary to obtain the desired accuracy in the study.)

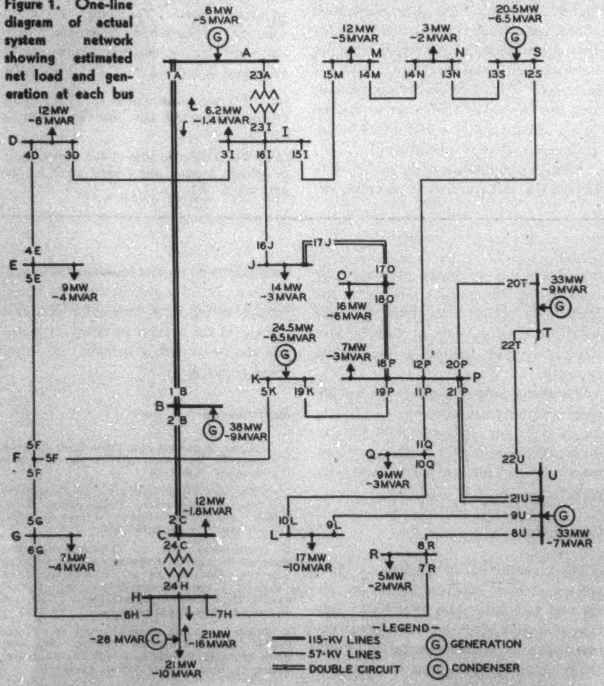


Figure 1. One-line diagram of actual system network showing estimated net load and generation at each bus

3. Loads and Estimated Generation

As shown in Table II and on Figure 1, the real and reactive power should be tabulated at all load busses. Wherever generator or synchronous condenser inputs to the network occur at load busses, the input and load real and reactive power should be combined to give the net load or generation at that point. It should be noted that in Table II the leading reactive power at load busses is moved into the generation column, so that all reactive will be at the same algebraic sign, negative.

In the problem the voltage at the electrical load center of the system is 57.7 kv, so that the reference voltage to neutral is chosen as $33.33+j0$ kv. Bus P on Figure 1 is the main receiving station for the generating plants and would be expected to have a voltage phase angle approximately that of the major loads, so that it is chosen as the reference bus. Therefore all voltage and current components for the network will refer to this same reference voltage, $E_n'=33.33+j0$ kv. Since the components of voltage to neutrals E_p and E_q are unknown at the other load busses of the network, E_n' is assumed to exist at all load busses, so that the current components I_p and I_q can be computed as shown in Table II. At generator busses remote from the load center, the values of I_p and I_q are computed on the basis of estimated inputs to the network and estimated voltages, but, as shown in Table II, these input currents in total must equal the sum of the load currents.

4. Setup on D-C Board to Determine Voltage Drops

The resistance units of the d-c board are now set up to simulate the resistance or R network on the basis of the R values of Table I. The positive bus of the board is connected through a rheostat to a common bus from which all I_p input busses are fed through individual rheostats. The negative bus of the board is connected to a common bus to which all output I_p currents flow from their respective load busses of the network through rheostats. The board is then ready for adjustment to set up I_p input and output currents in some convenient proportion to the actual I_p currents shown in Table II. It is important to es-

Table I. Network Problem—Circuit Constants on 57-Kv Base

Circuit Designation	Resistance, R Ohms to Neutral	Reactance, X Ohms to Neutral	Conductance $G=R\div(R^2+X^2)$	Susceptance $B=X\div(R^2+X^2)$
1*AB*	0.5	2.0	0.1175	0.4710
2*BC*	1.0	3.5	0.0755	0.2640
3*ID*	2.5	4.0	0.1125	0.1798
4*DE*	12.0	12.5	0.0400	0.0417
5*EF*	3.5	4.0	0.1240	0.1420
5*FK*	3.5	4.5	0.1077	0.1384
5*FG*	15.0	23.0	0.0199	0.0305
6*GH*	9.0	14.5	0.0309	0.0498
7*HR*	12.0	15.5	0.0313	0.0405
8*RU*	17.0	24.0	0.0197	0.0278
9*UL*	9.0	13.5	0.0342	0.0513
10*LQ*	5.0	7.0	0.0676	0.0946
11*QP*	1.5	4.0	0.0822	0.2190
12*PS*	11.0	14.5	0.0332	0.0438
13*SN*	8.5	10.5	0.0467	0.0577
14*NM*	8.0	9.5	0.0520	0.0617
15*MI*	3.5	5.0	0.0940	0.1340
16*IJ*	4.5	7.0	0.0650	0.1010
17*JO*	1.5	2.0	0.2400	0.3200
18*OP*	1.0	1.5	0.3080	0.4620
19*PK*	2.5	7.0	0.0453	0.1267
20*PT*	11.5	34.0	0.0089	0.0264
21*PU*	4.5	9.0	0.0450	0.0890
22*TU*	5.0	15.0	0.0200	0.0600
23*AI*	0.5	4.5	0.0244	0.2192
24*CH*	1.0	8.0	0.0154	0.1231

Table II. Network Problem—Loads and Generation for Setup on D-C Calculating Board

Bus	Loads		Generation (Estimated)		Load Current		Generated Current	
	Megawatts	Megavars	Megawatts	Megavars	I_p	I_q	I_p	I_q
A	—	—	8.0	− 5.0	—	—	80	−*j*41
B	—	—	38.0	— 9.0	—	—	380	−*j*74
C	12.0	− 1.8	—	—	120	−*j*18	—	—
D	12.0	− 6.0	—	—	120	−*j*60	—	—
E	9.0	− 4.0	—	—	90	−*j*40	—	—
F	—	—	—	—	—	—	—	—
G	7.0	− 4.0	—	—	70	−*j*40	—	—
H	21.0	*	—	−16.0	210	*	—	−*j*135
I	6.2	*	—	− 1.4	62	*	—	−*j*12
J	14.0	− 3.0	—	—	140	−*j*30	—	—
K	—	—	24.5	− 6.5	—	—	245	−*j*54
L	17.0	−10.0	—	—	170	−*j*100	—	—
M	12.0	− 5.0	—	—	120	−*j*50	—	—
N	3.0	− 2.0	—	—	30	−*j*20	—	—
O	16.0	− 6.0	—	—	160	−*j*60	—	—
P	7.0	− 3.0	—	—	70	−*j*30	—	—
Q	9.0	− 3.0	—	—	90	−*j*30	—	—
R	5.0	− 2.0	—	—	50	−*j*20	—	—
S	—	—	20.5	− 6.5	—	—	193	−*j*52
T	—	—	33.0	− 9.0	—	—	300	−*j*73
U	—	—	33.0	− 7.0	—	—	304	−*j*57
Totals	150.2	−49.8	157.0	−60.4	1,502	−*j*498	1,502	−*j*498
Estimated loss =	6.8	−10.6						
Estimated input =	157.0	−60.4						

*Leading or + megavars at loads is shown as − megavars generation.

At loads $I_p=1{,}000\times\text{mw}/57.7\sqrt{3}=10\times\text{mw}$.

At loads $I_q=1{,}000\times\text{mvar}/57.7\sqrt{3}=10\times\text{mvar}$.

For input or generation to network, estimated bus voltages are used to compute I_p and I_q from the estimated megawatts and megavars input, but it is essential for the d-c board setup that the totals of I_p and I_q input are equal to the totals supplied to loads.

tablish a small current through the board at the start by adjusting the rheostat between the positive bus and the common input bus to have a high value of resistance as compared to the network resistances, so that it will act as a current limiter or, in effect, help maintain an almost constant total current to the network. Actually it would be better if a constant current supply could be used, but the current limiting rheostat referred to is satisfactory for the purpose. After the I_p currents into the network at generator busses and out of the network at load busses have been adjusted, by means of their respective generator and load rheostats, to be proportional to the desired values of Table II, the resistance of the current-limiting rheostat may be reduced to the point where a substantial current flows through the network. With more current it is then possible to adjust generator and load rheostats slightly to give the values of currents, still proportional to those of Table II, which are to be the basis for the readings of $I_{pr}R$ voltage differences between bus *P* and all other busses of the network. For example $(I_{pr}R)$ measured from bus *A* to bus *P* multiplied by the constant for the d-c board for this set of readings is equal to −635 volts and is recorded in Table III. The board constant depends on the proportionality factors between board resistances and actual network resistances, between board currents and actual network currents, and also on the type of scale calibration on the voltmeter, so that the data for this study are simplified by the elimination of the board constants from the tabulations.

In a similar manner the circuit resistance units are adjusted to simulate the reactance or *X* network on the basis of the *X* values of Table I. Again by use of the current-limiting rheostat the board current is reduced, and some minor adjustments in generator I_q and load I_q currents will make them proportional to those of Table II, and the same procedure as described for obtaining $I_{pr}R$ voltage readings is followed to obtain $I_{px}X$ voltage differences between all busses and reference bus *P*. These readings are in Table III.

In like manner $(-I_{qx}X)$ readings can be obtained. However it will be noted in this problem, and in most problems of

Table III. Network Problem—Measured Components of Voltage Drop Between All Busses and Reference Bus *P*

*	$(+I_{pr}R)$	$(-I_{qx}X)$	$V_p=I_{pr}R-I_{qx}X$	$(+I_{px}X)$	$(-I_{qr}R)$	$V_q=I_{px}X+I_{qr}R$
AP	−635	+865	+230	+125	+335	−210
BP	−595	+1,055	+460	+340	+380	−40
CP	−895	+1,135	+240	−605	+435	−1,040
DP	−960	+40	−920	−1,130	+80	−1,210
EP	−760	+70	−690	−820	−70	−750
FP	−405	+235	−170	−315	+25	−340
GP	−1,200	+620	−580	−1,840	+110	−1,950
HP	−1,075	+1,435	+360	−1,825	+505	−2,330
IP	−710	+260	−450	−735	+255	−990
JP	−550	−60	−610	−730	−10	−720
KP	+195	+345	+540	+445	+85	+360
LP	−275	−455	−730	−395	−305	−90
MP	−820	+40	−780	−900	+90	−990
NP	−120	+90	−30	−100	+120	−220
OP	−280	−90	−370	−440	−50	−390
QP	−170	−240	−410	−375	−105	−270
RP	−575	+865	+290	−880	+230	−1,110
SP	+890	+360	+1,250	+1,120	+320	+800
TP	+1,730	+1,050	+2,780	+4,410	+380	+4,030
UP	+1,020	+450	+1,470	+1,785	+175	+1,610

* $(+I_{pr}R)$ is measured with $+I_p$ currents flowing through *R* network.
$(-I_{qx}X)$ is measured with $+I_q$ current flowing through *X* network.
$(+I_{px}X)$ is measured with $+I_p$ current flowing through *X* network.
$(-I_{qr}R)$ is measured with $+I_q$ current flowing through *R* network.

Table IV. Network Problem—Computation of Components of Current Flowing in Each Circuit

Circuit Designation	Voltage Difference		$I_p=GV_p+BV_q$			$I_q=GV_q-BV_p$		
	V_p	V_q	GV_p	BV_q	I_p	GV_q	BV_p	I_q
1*BA*	+230	+170	+27.0	+80.0	+107.0	+20.0	+108.0	−88.0
2*BC*	+220	+1,000	+16.8	+264.0	+280.8	+75.5	+58.1	+17.4
3*ID*	+470	+220	+52.9	+39.6	+92.5	+24.7	+84.4	−59.7
4*ED*	+230	+460	+9.2	+19.2	+28.4	+18.4	+9.6	+8.8
5*FE*	+520	+410	+64.5	+58.2	+122.7	+50.8	+73.9	−23.1
5*KF*	+710	+700	+76.4	+97.0	+173.4	+75.4	+98.5	−23.1
5*FG*	+440	+1,610	+8.8	+49.1	+57.9	+32.0	+13.4	+18.6
6*HG*	+970	−380	+30.0	−18.9	+11.1	−11.1	+48.4	−60.2
7*HR*	+70	−1,220	+2.2	−49.5	−47.3	−38.3	+2.8	−41.1
8*UR*	+1,180	+2,720	+23.2	+75.8	+99.0	+53.6	+32.9	+20.7
9*UL*	+2,200	+1,700	+75.3	+87.2	+162.5	+58.1	+113.0	−54.9
10*QL*	+320	−180	+21.6	−17.0	+4.6	−12.2	+30.2	−42.4
11*PQ*	+410	+270	+33.7	+59.1	+92.8	+22.2	+90.0	−67.8
12*SP*	+1,250	+800	+41.6	+35.0	+76.6	+26.6	+54.8	−28.2
13*SN*	+1,280	+1,020	+52.0	+59.0	+111.0	+47.6	+74.0	−26.4
14*NM*	+780	+770	+40.6	+47.5	+88.1	+40.0	+48.2	−8.2
15*IM*	+360	0	+33.9	0.0	+33.9	0	+48.2	−48.2
16*IJ*	+160	−270	+10.4	−27.3	−16.9	−17.6	+16.2	−33.8
17*OJ*	+240	+330	+57.6	+105.8	+163.4	+79.1	+76.9	+2.2
18*PO*	+370	+390	+114.1	+180.2	+294.3	+120.4	+170.8	−50.4
19*KP*	+540	+360	+24.5	+45.6	+70.1	+16.3	+68.5	−52.2
20*TP*	+2,780	+4,030	+24.9	+106.5	+131.4	+36.0	+73.4	−37.4
21*UP*	+1,470	+1,610	+66.1	+143.4	+209.5	+72.5	+131.0	−58.5
22*TU*	+1,310	+2,420	+26.2	+145.0	+171.2	+48.4	+78.6	−30.2
23*AI*	+680	+780	+16.6	+171.0	+187.6	+10.0	+149.0	−130.0
24*HC*	+120	−1,290	+1.8	−159.0	−157.2	−19.9	+14.8	−34.7

this type, there is some modification of the connections between the network and the input and output busses for the I_q current setup due to the fact that loads with leading or positive reactive power are set up as generators of negative reactive power. In this problem this change is necessary for the connections to busses *H* and *I*. The reason why the voltage differences between all busses and reference bus *P* are designated as $-I_{qx}X$ voltages is that $+I_q$ currents are made to flow in at the input bus and out at the output bus, whereas actually they should be set up as $-I_q$ currents to conform to the signs of Table II.

Similarly by using the *R* network and $+I_q$ currents the $-I_{qr}R$ voltage differences are obtained.

Referring again to Table III, the inphase voltage difference between all busses and the reference bus *P* is $V_p=I_{pr}R-I_{qx}X$ for each case, and the quadrature component of voltage difference is $V_q=I_{px}X+I_{qr}R$.

5. COMPUTATION OF COMPONENTS OF CURRENT FLOWING IN EACH CIRCUIT OF THE NETWORK

Referring to Table IV, the voltage differences between the bus terminals of each circuit are derived from the V_p and V_q values of Table III. For example, V_p for circuit 1*BA* is obtained by subtracting V_p for *AP* from V_p for *BP* in Table III which gives +460−230=+230 volts. In like manner, V_p and V_q for all circuits of the network are derived.

Next, as shown in Table IV, the equations $I_p=GV_p+BV_q$ and $I_q=GV_q-BV_p$ are used to determine the inphase and quadrature components of current which must flow in each of the circuits of the actual a-c network to give the V_p and V_q voltage differences derived from the d-c board setups. The values of conductance *G* and susceptance *B* are those shown in Table I for each circuit of the network.

6. COMPUTATION OF BUS VOLTAGES AND REAL AND REACTIVE POWER

As noted at the start of the solution, the reference bus *P* was established as having a voltage to neutral of 33.33+*j*0 kv. From Table III the voltage differences to neutral of components V_p and V_q were obtained between all busses and bus *P*. To determine the voltage to neutral at every other bus, these voltage differences are added algebraically to the reference voltage. This is done, and the results are tabulated as E_p and E_q in Table V.

The current components I_p and I_q in Table V are not exactly the same as the current components given in Table II which were the basis of the d-c board setup, but instead are the algebraic sum of the components of current away from each generator bus and toward each load bus, so that Kirchhoff's law will be satisfied. For example at generator bus *A* the actual input current is equal to the sum of currents 1*AB* and 23*AI* expressed in their components. From Table IV, I_p for 1*AB* is −107.0 amperes, and I_p for 23*AI* is +187.6 amperes, and the algebraic sum is +80.6 amperes as shown in Table V. All I_p and I_q current components given in Table V are obtained in this manner.

The computations of real and reactive power in megawatts and megavars are obtained by using equations megawatts = 0.003 $(E_pI_p+E_qI_q)$ and megavars = 0.003 $(E_pI_q-E_qI_p)$ as shown in Table V.

Table VI is added to show the computation of the phase voltages at all busses and to give a direct comparison of the derived values of real and reactive power, which completely satisfy the current distribution of Tables IV and V and the voltages of Tables V and VI, with the problem setup values of Table II.

The comparison shows that for all

Table V. Network Problem—Computation of Real and Reactive Power

Bus	E_p Kv	E_q Kv	I_p Amperes	I_q Amperes	Real Power in Megawatts = $0.003(E_pI_p+E_qI_q)$	Reactive Power in Megavars = $0.003(E_pI_q-E_qI_p)$
A	33.56	−0.21	80.6	−42.0	8.16	−4.18
B	33.79	−0.04	387.8	−70.6	39.31	−7.09
K	33.87	+0.36	243.5	−75.3	*28.33	*−9.05
S	34.58	+0.80	187.6	−54.6	19.37	−6.12
T	36.11	+4.03	302.6	−67.6	31.97	−10.98
U	34.80	+1.61	299.8	−62.5	30.99	−7.97
C	33.57	−1.04	123.6	−17.3	12.53	−1.36
D	32.41	−1.21	120.9	−50.9	11.95	−4.50
E	32.64	−0.75	94.3	−31.9	*9.33	*−2.92
F	33.16	−0.34	−7.2	−18.6	*(−0.70)	*(−1.86)
G	32.72	−1.95	69.0	−41.6	*7.02	*−3.68
H	33.69	−2.33	193.4	+136.0	18.58	+15.12
I	32.88	−0.99	78.1	+11.7	7.67	+1.39
J	32.72	−0.72	146.5	−31.6	14.47	−2.80
L	32.60	−0.09	167.1	−97.3	16.35	−9.46
M	32.52	−0.99	122.0	−56.4	12.09	−5.16
N	33.30	−0.22	22.9	−18.2	2.30	−1.81
O	32.96	−0.39	130.9	−52.6	13.02	−5.05
P	33.33	0.0	100.5	−58.9	10.05	−5.88
Q	32.92	−0.27	88.2	−25.4	8.75	−2.44
R	33.62	−1.11	51.7	−20.4	5.28	−1.89

* Bus *F* was not set up as a load so that the real and reactive power at *F* may be moved to busses *K*, *E*, and *G* in amounts proportional to the admittance of the circuits between *F* and its adjoining busses. This is done in Table VI.

practical purposes the first solution is adequate for the analysis of this network problem. The greatest difference between derived and setup values of real and reactive power occurs at load buses *O* and *P* which are connected by a very low impedance circuit of $(1+j1.5)$ ohms. If the two loads are added together, the derived total is $23.07-j10.93$ megavolt-amperes as compared to a setup total of $23.0-j9.0$ so that the effect of the differences of the two loads would be negligible in the network. However, in case greater accuracy is desired, it is possible to repeat the solution with corrected values of I_p and I_q to obtain derived values of real and reactive power which check almost exactly with the setup values. The method of correcting the I_p and I_q currents is described in the section which follows, but the solution is not repeated, because it would add nothing of value to the paper.

Procedure for Second Solution

Unless a particularly accurate second solution were desired, the values of real and reactive power tabulated in Table VI would be compared, and the difference in the derived and setup values would form the basis for estimating the changes in I_p and I_q which would result in improved accuracy. However, a more exact procedure is given in the following example.

At bus *O* from Table II the load setup was $16.0-j6.0$ megavolt-amperes instead of the derived value of $13.02-j5.05$ megavolt-amperes, which satisfied the current distribution and voltages of the first solution. The voltage components for bus *O* from Table V are $E_p=+32.96$ and $E_q=-0.39$, and since these voltage components will not change appreciably in the second solution, the values of I_p and I_q can be computed with the equations:

$$I_p=(E_pP-E_qQ)\ 1{,}000/(E_p^2+E_q^2)3$$

$$I_q=(E_qP+E_pQ)\ 1{,}000/(E_p^2+E_q^2)3$$

In these equations *P* and *Q* are in megawatts and megavars and E_p and E_q are in kilovolts to neutral.

Whence $I_p=160.6$ amperes and $I_q=-62.5$ amperes.

In Table II the current components to the load at bus *O* were set up $I_p=160$ and $I_q=-60$; however in Table V the derived values were $I_p=130.9$ and $I_q=-52.6$. This difference is due to variations in *X/R* ratios in the circuits of the network, and about the same difference would occur in the second solution. Therefore, a corrected value of $I_p=160.6+(160-130.9)=189.7$ would be used for the second solution. The corrected value of $I_q=-62.5+(-60+52.6)=-69.9$ would also be used for the second solution. This same procedure could be followed at all other busses of the network to obtain corrected values for a second and more accurate solution to the problem.

Application of Superposed Method of Setup to the D-C Board

In cases where the d-c board does not have a sufficient number of rheostats to set up all of the generators and loads, it is possible to set up any proportion of the generators and loads at one time, so long as the total input current of the several generators adds up to the total output current to the several loads. The same voltage measurements from all busses to the reference bus would be required for each such setup, and the sum of the voltages thus obtained would give the same result as though all generators and loads had been included in one setup. This method requires additional readings, but because of the smaller number of generator and load rheostats which must be adjusted to obtain correct input and load currents, the current adjustment is simplified.

Table VI. Network Problem—Computation of Phase Voltage at Busses; Comparison of Result of Table V With Values Set Up in Table II

Bus	E_ϕ in Kv $=1.732\sqrt{E_p^2+E_q^2}$	Loads* Megawatts	Loads* Megavars	Generation* Megawatts	Generation* Megavars
A	**109.3	— —	— —	8.16 (8.0)	−4.18 (−5.0)
B	**140.0	— —	— —	39.31 (38.0)	−7.09 (−9.0)
C	**109.4	12.53 (12.0)	−1.36 (−1.8)	— —	— —
D	56.3	11.95 (12.0)	−4.50 (−6.0)	— —	— —
E	56.6	8.99 (9.0)	−3.82 (−4.0)	— —	— —
F	57.5	— —	— —	— —	— —
G	56.8	6.96 (7.0)	−3.84 (−4.0)	— —	— —
H	58.6	18.58 (21.0)	— —	— —	−15.12 (−16.0)
I	57.0	7.67 (6.2)	— —	— —	−1.39 (−1.4)
J	56.8	14.47 (14.0)	−2.80 (−3.0)	— —	— —
K	58.7	— —	— —	28.63 (24.5)	−8.25 (−6.5)
L	56.5	16.35 (17.0)	−9.46 (−10.0)	— —	— —
M	56.4	12.09 (11.8)	−5.16 (−5.0)	— —	— —
N	57.7	2.30 (3.0)	−1.81 (−2.0)	— —	— —
O	57.2	13.02 (16.0)	−5.05 (−6.0)	— —	— —
P	57.7	10.05 (7.0)	−5.88 (−3.0)	— —	— —
Q	57.1	8.75 (9.0)	−2.44 (−3.0)	— —	— —
R	58.4	5.28 (5.0)	−1.89 (−2.0)	— —	— —
S	60.1	— —	— —	19.37 (20.5)	−6.12 (−6.5)
T	62.9	— —	— —	31.97 (33.0)	−10.98 (−9.0)
U	60.3	— —	— —	30.99 (33.0)	−7.97 (−7.0)
Totals	—	148.99	−48.01	158.43	−61.10

* Problem values set up in Table II are given in brackets.

** Turns ratio of 115/57-kv transformers = 1.88/1.

Use of Method With D-C Board With Fixed Resistors

If a d-c board is of the fixed resistor type, it may be possible to adapt it for use with this method, provided it has enough rheostats to use for generator and load-current adjustment.

Most networks have many circuits with *X/R* ratio almost equal. If the board is set up to simulate *X* only, this network is already available. Assume that 75 per cent of the circuits of a particular network have an *X/R* ratio of 2/1 or approximately so, and that only 25 per cent vary much from this ratio. If a means is provided for changing this 25 per cent to have the 2/1 ratio for setups requiring the *R* network, then the method may be used. The board constant for the *R* network would then be one-half that of the *X* network.

Use of Method Where Line Capacitance Must Be Considered

In the network problem of this paper, the effect of shunt capacitance of the lines was neglected, because the amount of leading megavars of the circuits would not change the solution appreciably. However, if it is necessary to consider line capacitance, it is suggested that the capacitance + megavars for each line be computed on the basis of estimated voltages at the start of the problem. In the usual manner one half of the megavars of all lines terminating at a bus would be allocated at that bus, and an adjustment in net load or generator megavars at that bus would be made to obtain the corrected megavars from which the I_q component of current would be computed.

Setup of Transformers With Different Ratios in a Loop Circuit

In a paper presented before the Portland section of the AIEE, the writer included an example to show how a series boost or buck may be provided from a separate source of direct current inserted at the point of boost or buck in the loop circuit, but rather than lengthen this paper, the method will be indicated only.

In most networks the quadrature component of voltage with respect to the reference voltage is small as compared to the inphase component, so that the effect of any normal boost or buck on the E_q component is negligible. However, if one transformer has a ten per cent boost or buck with respect to other transformers operating in closed paths with this transformer of different ratio, then a separate d-c source should be inserted at the point of boost or buck in the X network setup with I_q current flowing. This gives the desired effect to the inphase voltage readings for V_p. In the problem of this paper the voltage to neutral is approximately 33 kv at bus I, so if a boost of ten per cent were required in transformers 23AI, the series potential required would be 3,300 divided by the board constant. It would be inserted between bus A and the resistance unit representing X of the transformers 23AI. If the potential source had appreciable internal resistance, the 23AI unit setting would be reduced by this amount. As noted before, it would be used only in the setup for reading $I_{qx}X$ voltage differences.

Setup Using Ammeter Instead of Voltmeter on D-C Board

Although the measurement of the $I_{pr}R$, $I_{px}X$, $I_{qr}R$, and $I_{qx}X$ voltages between all busses and the reference bus is recommended, it is possible to read current distribution in each of the four network setups on the d-c board and compute the voltage differences for all circuits in each setup, and obtain the same results.

Conclusions

The scope of this paper has been quite limited with respect to the broad field of a-c network analysis, but it is believed that the network problem and the discussion show how this new method can be applied with the aid of a conventional d-c board to solve complex problems of load distribution and voltage conditions for a-c networks in a relatively simple manner and with good speed.

In the application of the method to the conventional d-c board, it probably will become apparent that various combinations of resistance networks could be set up simultaneously, which, together with proper metering arrangements could be used to reduce the analysis from four board setups to one—thus eliminating the greater part of the computation work. Patent application has been made for the method as applied to such simplifying resistance network combinations.

As an indication of the time required for a study of this type, the writer unaided set up the board and took the four sets of readings required in eight hours. An additional six hours was required to make all of the computations and tabulate the results. If the outline of procedure given in the paper is followed, it is believed that other engineers will find the method to be a useful tool in their system planning work.

References

1. ALTERNATING-CURRENT PHENOMENA (fifth edition, 1916), C. P. Steinmetz. Symbolic Method. Page 36.

2. THE ENGINEER'S MANUAL (second edition, 1939), Ralph G. Hudson. Harmonic Alternating Currents. Pages 214–21.

3. ELECTRICAL CHARACTERISTICS OF TRANSMISSION CIRCUITS (third edition, 1926), William Nesbit. Paralleling of Transmission Circuits. Pages 183–4.

4. LOAD STUDIES ON THE D-C CALCULATING TABLE. W. C. Hahn. *General Electric Review*, part 1, July 1931, page 444; part II, August 1931, page 482. (This is very complete and shows how the d-c board may be used for load studies, but it follows an entirely different approach to the problem of d-c board setup than that given in the present paper.)

Design, Manufacture, and Installation of 120-Kv Oil-Filled Cables in Canada

D. M. FARNHAM
MEMBER AIEE

O. W. TITUS
MEMBER AIEE

Synopsis: This paper describes the 120-kv oil-filled cable system of the Montreal Light, Heat, and Power Consolidated, installed during 1941 and in present operation as a link in a large interconnection scheme.

The paper describes the functions of the various sections of the system and the reasons for the choice of this method of effecting the interconnection. The steps required in designing the cable system are outlined and the general principles governing each such step discussed.

A short section deals with manufacture followed by a section covering the organization required for installation and the methods followed. The final section deals with operating practice, especially the basis of loading.

System

THE 120-kv oil-filled cable system of the Montreal Light, Heat, and Power Consolidated as at present installed may properly be divided into the following sections: (Figure 1):

1. Station *A* to station *B*—two circuits
2. Station *B* transformer cables—two circuits
3. Station *B* to station C—one circuit

The first and third parts are much the most important in size, but the second part, while relatively small, presents a use of high-voltage cable of particular interest to transformer station engineers.

In sequence of installation one circuit plus a spare phase of section 1 were purchased and installed initially on one contract, the remaining two phases of that section and all of sections 3 on a second contract. Section 2 was on a third contract but was installed during the same period as section 3.

Section 1 (known as circuits *A* and *B*), takes power from the 110-kv bus of station *A* directly to the new station *B* for service to an important business and industrial section of the city. Station *B* departs from the general substation scheme of the utility, in that it is not part of their 60-kv ring system, but is fed practically direct from the generating source, station *A* acting as a switching station. The present availability of underground cable suitable for 110-kv operation permitted locating the new station at its proper load center without double transformation or further loading or reinforcement of the already busy 60-kv ring. It relieves the ring as well and permits further load growth with existing facilities.

Section 2 takes power from the 110-kv bus of station *B* to the transformers, at present two, with provision for an ultimate of four. Underground feed was chosen as providing a substantial saving in steel structure, greater compactness and better appearance and much less exposed high-voltage circuit. Also when combined with underground cable on the low-voltage side, it permits a virtually "dead-front" station, excepting under the easily isolated and relatively small high-voltage bus section which lies in one 25-foot-wide strip along the south side of the station property.

Section 3 (known as circuit *C*), takes power from the 110-kv bus of station *B* to station *C* and acts in combination with circuits *A* and *B* as a direct tie between the Beauharnois system of the Montreal Light, Heat, and Power Consolidated supplying station *A* and the system of the Shawinigan Water and Power Company, and through it to the Saguenay Power Company system. It also will serve as an additional circuit to station *B*.

This direct tie between the first two systems was made in order to place the entire generating capacities of these three large companies on one interconnection, from which power for wartime industrial requirements could be drawn by the industry most vitally needing it.

The Shawinigan and Saguenay systems were already connected together by high-voltage transmission lines with ample capacity to carry any loads expected. Between the Beauharnois and Shawinigan systems the connection consisted of a 60-kv double ring tied through transformers to the 110-kv system of the Shawinigan Company and through other transformers to the 110-kv Beauharnois system (Figure 2). Thus the power that could be transferred from the Beauharnois system to the interconnection of the Shawinigan and Saguenay systems was limited by the Montreal Light, Heat, and Power Consolidated 60-kv ring and transformer capacity which was partially used by the primary power requirements of the Montreal Light, Heat, and Power Consolidated system.

The original suggestion was to construct an overhead 110-kv tie around the metropolitan area of Montreal between the Beauharnois system and the Shawinigan system. This would have involved the building of about 15 miles of steel tower transmission line over territory where the right-of-way problems were difficult and uncertain, especially as to the cost and time required to negotiate.

The second suggestion was to extend the 120-kv oil-filled cable system then nearing completion under the first contract mentioned above (circuit *A* of section 1) to tie with the 110-kv transmission system of the Shawinigan Company. This required the adding of two phases between stations *A* and *B*, which combined with the spare phase already installed for circuit *A* to become circuit *B* plus about five miles of additional cable circuit across the metropolitan area.

The cable tie was selected on the basis of several factors, most important among which were:

Initial cost
Time required
Stability
Freedom from lightning troubles
Present and future public reaction

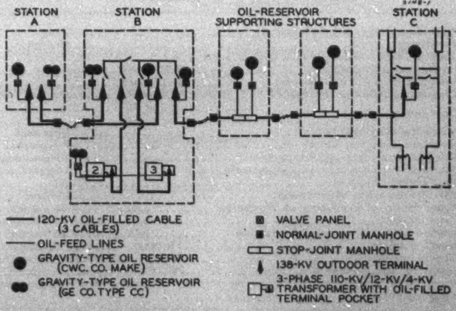

STATION A
STATION B
OIL-RESERVOIR SUPPORTING STRUCTURES
STATION C
2
3
120-KV OIL-FILLED CABLE (3 CABLES)
OIL-FEED LINES
GRAVITY-TYPE OIL RESERVOIR (CWC. CO. MAKE)
GRAVITY-TYPE OIL RESERVOIR (GE CO. TYPE CC)
VALVE PANEL
NORMAL-JOINT MANHOLE
STOP-JOINT MANHOLE
138-KV OUTDOOR TERMINAL
3-PHASE 110-KV/12-KV/4-KV TRANSFORMER WITH OIL-FILLED TERMINAL POCKET

Figure 1. Single-line diagram of system

Paper 42-148, recommended by the AIEE committee on power transmission and distribution for presentation at the AIEE Pacific Coast convention, Vancouver, British Columbia, Canada, September 9–11, 1942. Manuscript submitted June 1, 1942; made available for printing July 8, 1942.

D. M. FARNHAM is assistant distribution engineer, Montreal Light, Heat, and Power Consolidated, Montreal, Quebec, Canada. O. W. TITUS is chief engineer, Canada Wire and Cable Company, Ltd., Leaside, Ontario, Canada.

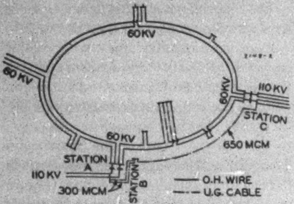


Figure 2. Interconnections between Montreal Light, Heat, and Power Consolidated and Shawinigan Water and Power Company systems

Since these three contracts constitute the most important underground cable installation in Canada and its highest-voltage underground system (the previously highest being 66-kv cables constituting part of the above 60-kv ring installed during 1927, 1931, and 1937 by the suppliers of the present cable), and since the type of cable is new to Canada, descriptions of the elements of an oil-filled cable system and the problems and steps in its design and installation seem warranted.

Cable-System Design

Underground cable systems above 75 kv fall into classifications of:

(*a*). Oil-filled cables in which practically all gas is removed from the cable, insulation, impregnant, joints, and terminals, ensuring that none of these elements are permitted to come into contact with a gas thereafter and so engineering the system that no part of it will be subjected to pressures less than atmospheric during service. It necessitates the use of an oil-impregnant fluid at all operating temperatures and oil channels giving access for the impregnant to all parts of the cable system.

(*b*). High-gas-pressure systems[1,2] in which ionization of gas pockets in the insulation is prevented by keeping them at such high gas pressures that their critical corona voltage substantially exceeds any electric stress gradients in the insulation. The usual pressures are 200 to 250 pounds per square inch. There are several such installations in service up to the 132-kv range but mostly at 66 kv. The pressures employed demand the use of steel as a containing element which gives the added advantage of excellent mechanical protection but requires great care in protection against corrosion, a serious problem where buried in earth over relatively long distances.

As the oil-filled class of system is well adapted to installation in conventional duct systems in busy city streets, and as there is in excess of 1,200 miles in service throughout the world at voltages up to 220 kv, this was the type chosen for this cable system. It is the type dealt with hereafter in this paper.

The design of an oil-filled cable system involves the following elements of choice, design and engineering:

1. Type of installation, that is, duct or buried directly in earth.
2. Number of conductors per cable.
3. Size of oil channel.
4. Size of conductor.
5. Thickness of insulation.
6. Thickness of sheath.
7. Spacing between oil-feed points.
8. Type of oil feed, that is, gravity or balanced pressure.
9. Normal joint design.
10. Pothead design.
11. Stop-joint design.
12. Size of oil-feed pipe lines.

1. Ducts

As the cable system lies beneath the streets of one of the largest cities of Canada and under some of the busiest streets of that city, the well-accepted practice of installing the cable in a duct system was the choice. Diameters for the 300,000-circular-mil and 650,000-circular-mil cables were approximately $2^{1}/_{4}$ and $2^{1}/_{2}$ inches respectively, for which $3^{1}/_{2}$ inch and 4 inch fiber ducts were chosen.

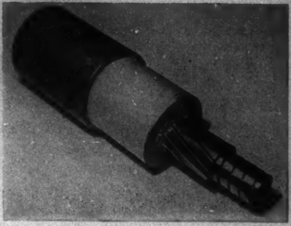


Figure 3. Cable construction

The duct-line route was selected for the most level contour (Figure 14), always desirable for oil-filled cable. Several surveys were made to determine such a route, which fortunately was one of the most direct, although through one of the busiest sections of the city.

The total length of the conduit run was approximately 36,000 feet. Work was commenced for the ducts for the second phase of the work, approximately 125,000 duct feet, in early June 1941 and completed by the end of September of the same year.

All manholes were built in such a way as to allow a minimum horizontal cable offset between duct mouth and joint of 42 inches. This was based on recom-

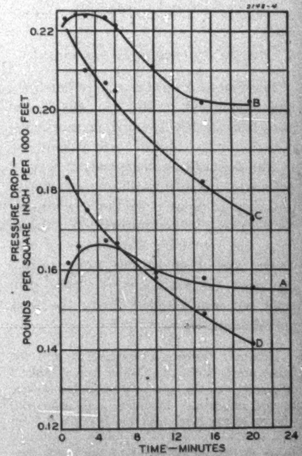


Figure 4. Curves of transient pressure drop per 1,000 feet versus time, due to dropping load and dielectric loss

Curve *A*—From full load
Curve *B*—From 75 per cent load
Curve *C*—From 40 per cent load
Curve *D*—From 25 per cent load

Table I

Circuits	Station A to B	Station B to C
Operating voltage (for calculations)	120 kv	
Frequency	60 cycles	
Number loaded cables per duct bank	6	3
Insulation thickness*	0.500 inch	0.500 inch
Lead thickness (for calculations)	0.125 inch	0.133 inch
Duct centers (for calculations)	$5^{1}/_{4}$ inches	$6^{1}/_{2}$ inches
Duct configuration	*A B C* *C B A*	*C* *B A*
Maximum daily load factor	75 per cent	
Maximum daily loss factor	$62^{1}/_{2}$ per cent	
Maximum ambient temperature of earth	20 degrees centigrade	
Maximum permissible copper temperature	70 degrees centigrade	
Duct thermal constant	1.04	
Thermal resistivity of insulation	550 ohms	
Power factor of insulation at 70 degrees centigrade	0.75 per cent (guaranteed value)	
Specific inductive capacity of insulation	3.7	
Sheath currents	Flowing	Not flowing
Current amperes	375	600
Conductor area circular mils	300,000	650,000

* See Table II.

mendations of Herman Halperin of Chicago, whose advice on this and other features is gratefully acknowledged.

Records of cable pulls on section 1 indicated that for straight runs a coefficient of friction of about 0.25 to 0.3 could be used for calculating cable pulls. On this basis it was decided to attempt to locate manholes up to 1,000 feet apart. As constructed, the maximum length is 975 feet, 8 inches, and the average length for section 3 is 814 feet. For this reason particularly rigid inspection of ducts was made to eliminate all bad or doubtful pieces of ducts or couplings. The duct supply, however, was found to be extraordinarily good.

Ducts were laid in a concrete envelope and in a manner to provide drainage to manholes. An attempt was made to maintain a 30-inch cover at least. In all but a very few cases the manholes have drainage connections to the city sewer system. In consequence, very little time had to be spent pumping water.

One section of duct run extends under a ship canal. Here the duct mass has a 6-foot cover to the bottom of the canal. Considerable care was taken in this construction to insure the best reinforced concrete job possible, not only because it passed under a ship canal, but further because the bottom on which the mass was constructed was quicksand. This section was built of 3,000-pound concrete reinforced with steel rods. The slump of the concrete mix was watched especially carefully.

2. Number of Conductors

Cable dimensions practically preclude the use of multiconductor cables at 120 kv. Three single-conductor cables per circuit consequently were the necessary decision, and design and calculations were based accordingly.

3. Oil Channels

There are in common use for single-conductor cables, two oil channel sizes, namely 0.500 inch and 0.690 inch.

The smaller diameter core results in a substantial saving in cable cost, but the larger core permits feeding nearly twice as far as the smaller core. The use of the smaller core increases substantially manhole, joint, reservoir, and miscellaneous accessory and installation costs. At the same time it restricts the choice of the type of reservoir and adds to the number of points requiring operating supervision and inspection.

It was decided to employ the 0.690-inch oil channel, especially since section 1 was the only part originally contemplated, and, as it was 8,800 feet long, this permitted its being fed from the ends alone.

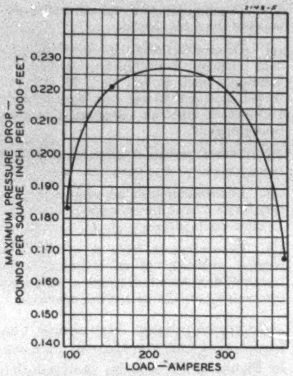


Figure 5. Curves of maximum transient pressure drops, versus load prior to interruption

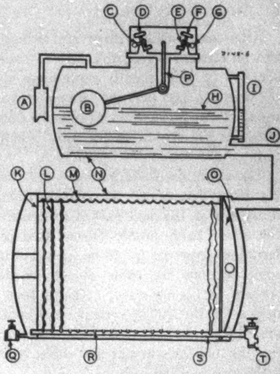


Figure 6. Schematic diagram of new gravity-type oil reservoir (Canadian design)

A.	Activated alumina dehydrator
B.	Float
C.	Oil-level relay
D.	Low level
E.	High level
F.	Mercoid switch
G.	Counterweight
H.	Gauge oil level
I.	Oil-level indicating gauge
J.	Pipe-connecting tanks
K.	End plate for cell unit
L.	Oil cells
M.	Cell spacers
N.	Galvanized steel tank
O.	End plate for cell unit
P.	Actuating arm
Q.	Gauge oil drain valve
R.	Cell oil pipes
S.	Stainless-steel tube manifold
T.	Packless diaphragm valve

4. Size of Conductor

The size of conductor was determined by accepted methods[3] based on thermal limitations. Bases for calculations were as set forth in Table I.

At the time of designing the system as far as station *B*, generous factors of safety for a single circuit determined the choice of 300,000-circular-mil conductors. The addition of the second circuit brought the capacity of this part of the system to approximately 140,000 kva.

A load of about 30,000 kva is expected at station *B* during the war period, leaving an available capacity of 110,000 kva or more to pass on to station *C*. It was therefore decided to continue the tie line for this capacity, which gave a cable size of 650,000 circular mils as indicated by Table I, the load and loss factors having been determined as follows:

The shape of the cable load curve was the Montreal, Light, Heat, and Power Consolidated's system load curve inverted and superimposed on a flat load curve whose value was the difference between the system peak and its generating capacity. This gave a load factor less than 75 per cent and a loss factor of above 50 per cent. Such being the case, it was decided to make all calculations on the more or less standard basis of 75 per cent load factor and 62$^1/_2$ per cent loss factor, which were expected would provide the additional capacity required to carry weekend surplus power.

5. Thickness of Insulation

As other system characteristics precluded a higher voltage than 120 kv ever being used, an insulation thickness of 0.500 inch was chosen.[4]

Table II. Cable Dimensions

		300,000 Circular Mils	650,000 Circular Mils
Steel channel	Inside diameter (inches)	0.69	0.69
	Outside diameter (inches)	0.79	0.79
Copper conductor	Outside diameter (inches)	0.98	1.17
Paper insulation	Thickness (inches)	0.50	0.50
	Outside diameter (inches)	1.98	2.17
Lead	Thickness (inches)	0.125–0.156	0.133–0.164
	Outside diameter (inches)	2.23 –2.29	2.44 –2.50
Weight per foot	Pounds	7.15 –8.46	8.16 –9.40

6. THICKNESS OF SHEATH

After consultation with several authorities on stresses permissible in lead cable sheaths (a lead sheath containing approximately 0.06 per cent of copper was used), it was decided not to exceed a fiber stress in the sheath under static conditions of 125 pound per square inch.

The elevation of the oil reservoirs having been determined by considerations dealt with in the next part of this paper, the actual cable profile showed that internal oil pressures would be such that in some sections the cable sheaths would require to be increased. Sheath thicknesses for the 300,000-circular-mil cable varied from 0.125 inch to 0.156 inch and for the 650,000-circular-mil cable from 0.133 inch to 0.164 inch.

Figure 3 illustrates the cable construction.

7. SPACING BETWEEN OIL-FEED POINTS

The making of this decision involved some of the most extensive and interesting calculations of the entire design. The governing consideration is that on dropping load the cable cools and demands oil. During this cooling period a "transient" drop in oil pressure occurs at all points away from the oil supply and is greatest at the point most remote from supply, that is, halfway between feed points.

It is a standard of oil-filled cable system design that no point in the system shall fall below one pound per square inch above atmospheric pressure (in order to prevent ingress of air or moisture if the cable sheath be punctured). When the "transient" drop for unit distance under worst conditions is known and correlated with the pressure head permissible without overstressing the sheath (which sets the reservoir elevation or pressure), the maximum feeding distance is determined.

Steps in determining the "transient" are, in brief:[5]

(*a*). Determination of the thermal capacitances of the cable elements.

(*b*). Determination of the thermal resistances of the cable elements.

(*c*). Determination of the rate of cooling of each element from representative initial conditions such as full load, 75 per cent, 50 per cent, and 25 per cent loads at minimum earth ambients (assumed 0 degrees centigrade).

(*d*). Determination of oil-volume demand (proportional to first power of *c* above).

(*e*). Determination of oil temperatures at each instant and the viscosities of the oil at these temperatures.

(*f*). Determination from *d*, *e*, and fluid friction coefficient of oil channel, of the transient drop for a unit length at successive intervals for each initial loading condition.

Curves of pressure drop against time for dropping from various loads are shown in Figure 4. It will be observed that maximum drop occurs as late as three minutes after zero time.

Figure 5 of maximum pressure drop versus initial loads is then drawn from the maximum values obtained from the curves of Figure 4. It will be seen that worst conditions obtain in dropping about one-half to two-thirds full load during winter conditions.

As pressure heads in the order of 15 pounds per square inch are quite safe for normal lead sheaths of these diameters, it was felt that there were ample margins of safety in using feeding distance of up to 5,000 feet, that is, spacing between feed points of 10,000 feet. This proved suitable for conditions in the field, as station *B* is 8,800 circuit feet from section *A*, and there were suitable locations for oil-reservoir structures at approximately 9,000 feet and 18,000 feet from station *B*, and station *C* lies about 27,000 feet from station *B*.

8. TYPE OF OIL FEED

Two general types of oil reservoirs are available to the design engineer.

(*a*). *Gravity type* in which the requisite pressure to reach all the system is obtained by locating the reservoir at an elevation determined by the cable profile and the feeding distance, as the oil in the reservoir is at atmospheric pressure (plus the slight back pressure of the cell walls). In practically all cases, excepting for unusual profiles, gravity reservoirs must be mounted above the ground surface and cannot be used unless above-surface space is available at the feed points.

(*b*). *Balanced Pressure type* in which the requisite pressure is obtained by gas confined within the reservoir shell (plus auxiliary gas tanks if necessary) and external to the oil cells. The use of such reservoirs requires careful gas volume calculations and adjustments and introduces certain extra operating and maintenance items. However, they can be mounted in manholes and must be used when above-surface reservoir space is not available.

In the systems covered by this paper above-surface space fortunately was available adjacent to desired feeding points. The gravity type of oil feed was chosen.

Figure 6 illustrates one type of gravity-type reservoir employed. This reservoir is a new development not previously used. It was designed and manufactured in Canada and has particularly low dead oil volume and cell back pressure. It is of 36 gallons (U. S.) capacity between plus 0.5 pound per square inch and minus 0.1

Figure 7 (left). Gang-treating normal joints

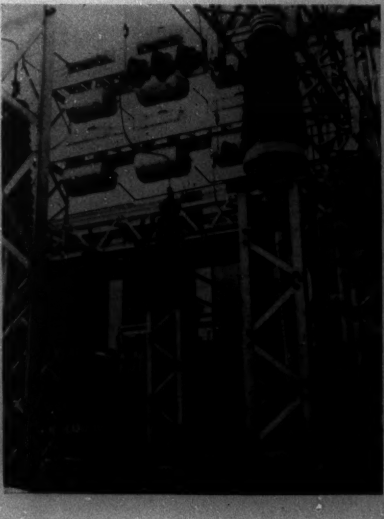


Figure 8 (right). Station *B* potheads, oil reservoirs, and oil pipe lines

Figure 9 (left). Stop joints, cradles, and piping (manhole 1040)

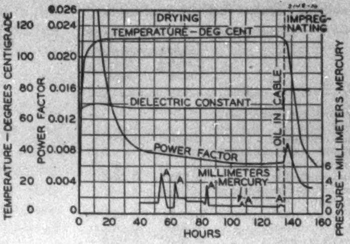


Figure 10 (right). Evacuation and impregnation curves

pound per square inch cell pressures. The smaller tank acts as a riser for the gauge oil to permit keeping the oil cells of the main reservoir completely immersed at all times and out of contact with the air.

The low dead oil capacity is due to both sides of each cell nesting together. Tests of numerous individual cells gave a life of 1,500 cycles not to failure. Each cycle was the full yearly travel of the cell. The daily working would not be 25 per cent of this amount.

9. Normal Joint

Normal joint design is similar to that for high-voltage solid-type cable with provision by means of hollow cores, three way valves, and oil ports for flushing out of any cable oil overheated during the soldering operations and establishing a permanent oil channel through the connector for servicing cable sections lying beyond the joint. Sharp depressions in the connector body which might set up high electric stresses were filled in by lead discs cast on the job and dressed carefully to the contour of the adjacent surface.

The initial joints were imported from the United States. The later ones were made in Canada (see Figure 7). Both were of designs in common use in the United States.

10. Pothead Design

Pothead design requires provision for relieving end stresses such as by combination of stress cones and barrier tubes, access to the cable core for oil to enter and leave the cable, and means of shutting off the core of the cable from the pothead body while erecting, evacuating, and filling the pothead. Two designs in common use in the United States were employed. On account of the low temperatures to be anticipated in the district, extra large oil ports were provided in the connector fittings. Here again part were manufactured in United States, but the later and larger quantities were made in Canada (see Figure 8).

11. Stop Joints

Stop joints serve two purposes. One is to segregate the oil systems where contours require that one section, having for example a higher elevation than an adjoining section, needs its reservoirs mounted at a higher elevation. At the same time troubles in one section are not communicated to adjacent sections. The second main function is that these joints act as a feed point and provide means of ingress and egress for the oil in the cable core. In the design utilized the stop joint essentially is a pair of cable terminals placed end to end, joined by a suitable connector block, and enclosed within a copper sleeve. Reinforcement insulation and enclosure within oil permits these terminals to be quite compact and readily mounted within a manhole. These were imported from United States.

Oil entry fittings are provided at each end of the joint to provide independent oil feeds to the cable sections on both sides of the joint. Impregnated wooden cradles support these long joints (see Figure 9).

12. Size of Oil-Feed Pipe Lines

Due to the very low temperatures likely to be encountered in the locality, and acting on the recommendation of Herman Halperin, it was decided to make exposed oil-feed lines from the reservoirs 1.5-inch copper pipe (Figure 8). Those parts of the oil-feed lines to the stop joints between the valve panel (where it is close to the foot of the structure) (Figure 9) and the stop joints were of one-inch soft copper pipe pulled into fiber ducts.

Manufacture

The manufacture of these oil-filled cables, although only slightly more complex, was different from the manufacture of solid-type cables, chiefly in that drying and impregnating of the paper insulation was done after the core was lead-sheathed. This is possible because of the presence of the oil channel within the conductor.

As may be seen from Figure 3 the conductor is of a novel make-up. Instead of the usual round wires, in single or multiple layers, 12 segments, carefully designed to fit snugly around the steel channel and present a smooth outer periphery, were stranded with a ten-inch right-hand lay.

An advantage for this conductor is that it inherently reduces the electrostatic stresses formed unless remedial measures are used at the crests of wires in conventional stranding.

After insulating, the core was heated at atmospheric pressure for five to six hours, sufficiently long to dry the insulation slightly and loosen the paper tapes so that the possibility of tearing the tapes during leading would be reduced. Leading followed immediately after the preliminary heating. The cable dimensions were as listed in Table II.

Evacuation and impregnation is effected through pipes wiped by fittings to the ends of the cable length, the sheath acting as the vacuum container. A typical impregnation curve with electrical and absolute pressure values is given in Figure 10.

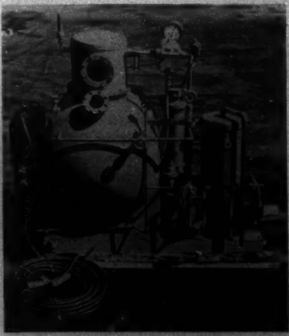


Figure 11. Portable degasser, operating side

Table III. Typical Test Values of Cable

	300,000 Circular Mils	650,000 Circular Mils
Insulation resistance, megohm miles at 15.5 degrees centigrade	6,000	5,000
Conductor resistance, ohms per 1,000 feet at 25 degrees centigrade	0.0355	0.0163
Power factor of insulation at 25 degrees centigrade:		
20 volts per mil	0.0038	0.0031
Working pressure	0.0043	0.0036
190 volts per mil	0.0046	0.0039
Ionization	0.0008	0.0008
Power factor of insulation at working pressure:		
22 degrees centigrade	0.0045	0.0040
80 degrees centigrade	0.0055	0.0048
60 degrees centigrade	0.0040	0.0043
40 degrees centigrade	0.0043	0.0038
Room temperature	0.0044	0.0040
Dielectric watts loss per foot, three-phase, 20 degrees centigrade	0.70	0.70
Dielectric constant at 35 degrees centigrade	3.94	3.94

After cooling, the cables were tested based on Association of Edison Illuminating Companies specifications.[4] Typical results are given in Table III.

Following the tests, the inside end of the cable length was connected through a flexible pipe to degassed oil storage reservoirs housed in the cable drum (Figure 13); then a pulling eye was fitted to the outside end of the length. The pulling eyes were provided with a threaded opening through which oil connections can be made.

With the pulling eye wiped on, the cable was ready for its final washing. A sufficient quantity of oil was washed through the cable core to remove any trace of burnt oil present in the cable because of the wiping operations incidental to fitting the pulling eye. When the power factor of the oil reached a value similar to that of the degasser storage tank oil, and the volume of oil the cable would expel from ten pounds pressure was under the specified limit showing the almost total absence of dissolved gas, the cable was made ready for shipment.

Installation

As this system was sold on an installed basis, the manufacturer was responsible for the cable and accessories until the cable went into operation. Under the manufacturer's engineer in charge, an organization was set up in which a cable-installation company contracted to do the handling, trucking, pulling, and such similar items, to supply top-grade skilled splicers, helpers, and laborers, and to supervise that phase of operation. Great credit is due to this company, as the success and speed with which the work was done was in no small measure due to their lengthy experience and to the high quality of workmanship of their skilled staff.

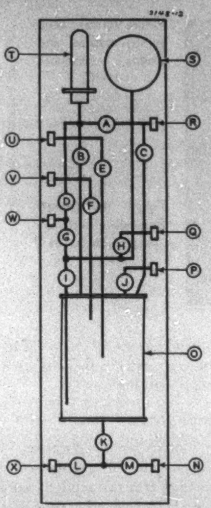


Figure 12. Treating bottle, valves, and connections

Treating panel packless diaphragm valves

A. Jumper vacuum
B. Bottle manometer
C. Bottle vacuum
D. Cable manometer
E. Oil return *A*
F. Oil return *B*
G. Cable oil
H. Oil line
I. Bottle oil
J. CO_2
K. Bottle bottom
L. Joint drain
M. Drain

Inlets and outlets

N. Drain outlet
O. Oil-test bottle
P. CO_2 inlet
Q. Oil inlet
R. Vacuum inlet
S. Compound gauge 30 inches–0–30 pounds
T. Absolute manometer
U. Oil return *A*
V. Oil return *B*
W. Vacuum oil outlet
X. Joint drain

Again under the engineer in charge was a staff of inspectors and engineers. The former checked each operation and dimension as performed. The latter were responsible for "treating" that is, evacuating and filling) all accessories, checking accessories as received, keeping reservoirs filled, and operation of the degasser. They were assisted by truck drivers who became skilled in handling the degasser and in many other duties. In addition, the utility maintained their own staff of inspectors checking and co-operating with the foregoing.

The installation of an oil-filled system involves planning for, training for, or executing the following:

1. Provision of suitable stock room, field office, tools, supplies and equipment, especially portable degassing equipment.
2. Provision for temporary source of degassed oil for feeding cable after pulling into ducts and before jointing.
3. Training of personnel, technical, semitechnical, and skilled tradesmen, in handling of cable and accessories, joint making and accessory installation.
4. Transport of cable and accessories from delivery point to point of installation.
5. Test check of cable or accessory for any leaks or imperfections before starting installation.
6. Actually installing, jointing, and so forth.
7. Continuous inspection and supervision of all operations.
8. Keeping of detail records of each individual operation, progress records, charts, and a daily log.
9. Making field check tests.

The basic considerations are that no air or other gas shall remain in, or in contact with, the cable oil, nor shall any overheated oil remain in the system. All training and inspection keep these prime necessities in view at all times. As detail procedure and instruction sheets are quite lengthy, this article can give only the general principles and procedure followed.

1. TOOLS AND EQUIPMENT

(*a*). A portable degasser, a vital piece of equipment, (Figure 11) serves as a source of high vacuum (less than one-millimeter absolute pressure) and as a source of warm degassed oil. The same special oil used in impregnating the cable is taken to the field in 50-gallon sealed drums. Here it is tested for breakdown strength and color, drawn into the portable degasser, filtered, and sprayed under high vacuum into the main tank of the degasser. It is then recirculated, through a rigidly controlled heat exchanger, and again sprayed into the high vacuum until it reaches approximately 60 to 65 degrees centigrade and is thoroughly degassed. A viewing window in the dome of the degasser permits ready inspection of the oil condition. Vacuum and pressure gauges, thermometers and thermostatic controls, automatic cutoffs, and similar safety devices make this degasser a highly reliable and efficient instrument. During the larger contract two degassers were employed, one mounted in a trailer, the second in a truck.

(*b*). A "treating bottle" and its associated valve panel (Figure 12) are employed at the joint or accessory being treated. These embody a system of valves and vacuum and pressure gauges by which the oil or vacuum from the degasser can be applied to the accessory being treated, the degree of treatment checked, and oil from the degasser checked for quality before being admitted to the cable system.

(*c*). Extra-heavy-walled one-half-inch (inside diameter) special rubber hose is used as vacuum line from the degasser to the work. Soft copper tubing three-eighths inch (outside diameter) is used as the oil line.

(*d*). A plentiful supply of three-eighths inch (outside diameter) soft copper tubing and flare fittings such as nuts, tees, crosses, unions, and so forth was maintained in the field stock room with the degasser and with the inspection crews.

Spare manometers, both atmospheric and absolute types, pressure-vacuum gauges, mercury, a small hand-operated high-grade vacuum pump, appropriate wrenches, spanners, and miscellaneous tools were found essential.

(*e*). Report forms were set up in great detail, individual forms covering the following:

Pulling.
Manhole and reservoir inspection.
Normal joints.
Stop joints.
Terminals.
Degasser log.
Truck log.
Progress charts.

2. Temporary Source of Degassed Oil

As the permanent reservoirs were available at the time cable started arriving at the site, these reservoirs were mounted at or close to their permanent locations. Cable deliveries were scheduled so that lengths adjacent to the reservoirs were first pulled in and connected to the reservoirs, the next lengths connected to the first by temporary oil lines, and so on. This eliminated the use of numerous temporary reservoirs and introduced many economies such as less frequent recharging, handling of reservoirs into and out of manholes, besides the investment cost in such temporary reservoirs. No troubles were encountered, nor was it felt that there was added risk with this technique as compared with practice common in United States.

3. Training of Personnel

Before jointing started, a school was instituted in which actual joints were made under simulated manhole conditions. Jointers and inspectors were required to do each operation of their respective duties in this school. All personnel handling the degasser were required to practice its operation until thoroughly conversant with it.

4. Test Checks

Each cable length at the site was checked by means of an "expulsion" test (Figure 13) to ensure that it was properly impregnated in the factory and that no gas had entered the cable thereafter.

CANADA WIRE & CABLE C

Figure 13. Expulsion test on reel length

Each reservoir, pothead, feed pipe, and valve panel, before installing and after installing but before filling with oil, was checked for leaks by evacuating and watching carefully for loss of vacuum when blanked off from the source of vacuum.

5. Installation

(*a*) *Cable.* Cable pulling was done by standard methods with careful records of pulling tensions and atmospheric conditions (some of this was done at temperature in the order of 0 degrees Fahrenheit). As the pulling eye enters the manhole toward which the cable length was being pulled, it was connected through a fitting screwed into the pulling eye to a source of degassed oil. The other end of the cable was then disconnected from the reel oil reservoir and pulling completed.

Table A. Coefficients of Pulling Friction

	At Average Temperatures of	
	Cold Weather Minus 7 C	Warm Weather 23 C
Maximum	0.520	0.370
Minimum	0.190	0.120
Average	0.312	0.258

Coefficients of friction computed from recorded pulling tensions are shown in Table A.

(*b*) *Reservoirs.* After erection and with valves installed, reservoirs were checked again by blank off. In the case of the Canadian-built reservoir of new design, calibration was effected as follows:

The cells were completely collapsed and degassed by being placed under vacuum. Gauge oil was then poured in through the riser tank until at the normal zero on the gauge glass. The volume of oil contained by the cells, when at operating zero, (minus 0.1 pound per square inch) is five gallons for these 36-gallon reservoirs. Accordingly, five gallons plus or minus an amount to allow for the contraction or expansion of the gauge oil between ten degrees centigrade (assumed normal temperature) and its temperature at the time of charging were drained from the gauge oil. The reservoir thus calibrated was then charged with degassed oil to whatever volume was required for the system at the time.

(*c*) *Oil Lines.* The permanent oil lines were of copper pipe with joints of the soldered capillary type. Each oil line was heated to remove moisture, tested by blank off for vacuum tightness, flushed with clean oil at 110 degrees centigrade and, if not for immediate use, was backfilled with dry carbon dioxide gas. When about to be placed in service the pipe was flushed with degassed oil, reevacuated, filled with degassed oil, and connected to a source of degassed oil fed from a reservoir.

(*d*) *Joints and Potheads.* As these were points at which the cable insulation and newly applied insulation were necessarily exposed to the atmosphere, special means were employed to remove such contamination. This paper, because of lack of space, does not go into details of splicing and erection, but outlines the added requirements above ordinary solid-type cable joints consequent on the type of cable and the principles followed in ensuring that the quality of the cable was not impaired.

1. All oil overheated by soldering operations must be flushed out.
2. There must not be a stoppage of oil channels when work is completed.
3. No sharp stress points may be left.
4. Proper contours must be maintained.

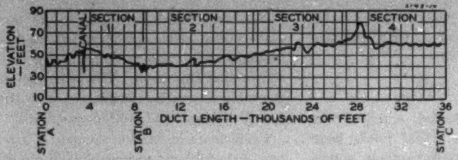


Figure 14. System profile of 120-kv duct line

5. There must be no leaks, even the most minute.

6. Dissolved and occluded gas must be removed.

7. The accessory must be left filled with good degassed oil.

These last two requirements are accomplished by the so-called "treatment." The joint or pothead was connected through the treating bottle to the portable degasser. Vacuum was applied until the accessory had reached a high degree of vacuum and so maintained until, on being blanked off, the completeness of the treating process was indicated by the lack of rise in absolute pressure. Drainage of oil coming out of the insulation during this operation was effected through a drain connection at the lowest point in the accessory. Such drainage, of course, must be into a vacuum.

The accessory was then filled with degassed oil from the portable degasser and connected to the system excepting in the instance of joints when this first filling was drained out, the bottom joint fitting permanently sealed, and the joint retreated and refilled with degassed oil. The top fittings of normal joints were then permanently sealed, as they have no source of oil other than from the cable itself.

In the 300,000-circular-mil section joints were treated immediately after the splicer finished. In the 650,000-circular mil section the splicers completed the three joints in each manhole. The three joints were then "gang" treated (that is, all treated at the same time). (See Figure 7.) This routine permits most effective use of all personnel and equipment and results in substantial time saving provided the skilled mechanics do first-class work. During this phase of operations the gangs were organized for continuous work from 8 a.m. Monday to 8 a.m. Saturday, using three eight-hour shifts.

On account of the small number of stop joints and of other limiting circumstances in the case of potheads this routine was not followed in these cases. Each such accessory was treated as soon as the splicers were finished.

7. INSPECTION AND SUPERVISION

The keeping of detailed and continuous inspection of all operations and constant backchecking was a highly important element in the success of the work. It was found that the skilled mechanics welcomed this inspection and co-operated thoroughly with the inspectors. Besides entering it in the proper report, all unusual results or circumstances were required to be reported directly to the engineer in charge.

8. KEEPING OF RECORDS AND LOG

Detail reports of personnel doing the work, time in and out, time each subdivision of the operation was completed, values of pulling tension, vacua, weather conditions, and so forth were turned in by the inspectors or engineers at the completion of the operation. These reports were then typed by the office man, and from them and other data, the "daily log" and progress charts were entered. From these data it can be determined what man did any one operation and under what conditions. Progress charts and records served their usual function of assisting in seeing that no operation was neglected and in correlating progress on all parts of the work and on preparation of progress invoices.

9. FIELD CHECK TESTS

As work progressed, expulsion, impregnation, and oil-flow tests were made frequently. The former two tests checked that no air or other gas had become dissolved or locked in the system. The last named ensured that there was no stoppage or constriction in the oil channels, such as from a wrongly positioned connector valve in a joint, or from solder working into an oil channel.

Operation

Contracts for the first and second sections of this work were placed in May 1940. As they were approaching completion in May 1941, the contract for the third and much larger section was placed. The whole system received voltage tests and was placed in initial service on December 14, 1941, two weeks ahead of the original schedule.

In order to transmit as much power as possible when available, that is, during the off-peak hours, it was decided to rate the cables as follows: Since the original calculations were based on a $62^1/_2$ per cent daily loss factor, it seemed satisfactory to allow a total daily watts loss from all sources equal to that established by using the standard formula for current-carrying capacity and to use as maximum current that established by using the factory measured resistance of the conductor as indicating the copper area rather than the circular-mil area. Since more power was available between midnight and 8 a.m., curves were made to show the maximum current that could be carried during these hours, depending on the current carried during the previous 16 hours, and to give a loss factor of $62^1/_2$ per cent with no current higher than the maximum allowable by calculation.

Records of ground temperature, idle duct temperatures, sheath temperatures in manholes, duct-mouth movements, and oil demands are being taken, and it is hoped that in the future these will provide some valuable information.

Sheath-bonding transformers of the Halperin and Miller design are being installed on circuit *C* which will increase the present load-carrying ability of the cables a considerable amount and at the same time allow for the saving and transmission of the load that is presently taken by the sheath losses. Because of the time element these were not in the main contracts, but provision was made for these transformers by including solder-seal sheath insulators in the joint casings.

The oil level in reservoirs is supervised by a simple alarm system operated by high- and low-level switches at each reservoir. The high or low oil level, as soon as it exists, is known by a station operator who is able to report the section in trouble to the proper persons.

A further precaution is being taken with reservoirs in the form of a dehydrator attached to the reservoir breather. This consists of a cylinder filled with activated alumina through which the air in contact

Table IV. Bill of Principal Materials

Exclusive of Spares

	Produced in Canada	Imported
Cable		
300,000 circular mils	53,400 feet	
650,000 circular mils	81,500 feet	
Number of lengths	201	
Joints		
Normal	124	56
Stop		6
Terminals	21	9
Reservoirs		
36 gallons	24	
17 gallons		12
9 gallons		2
Oil-level relays	24	8

A New Single-Pole Service Restorer

E. E TUGBY
ASSOCIATE AIEE

Synopsis: The widespread and general use of three-phase automatic service-restoring equipment on low-cost low-revenue distribution lines has created a field for a single-pole service-restoring device having continuously adjustable characteristics for trip current and time delay, and powered by a prestored source of energy. The device described in this paper utilizes a new and unique application for the prestoring of the operating energy and the restoration of the utilized operating energy after transient fault conditions.

A STUDY of the requirements for a source of operating energy for this new single-pole service-restoring device indicated a need for a source that would be independent of fault current and have available a dependable source of prestored energy when a fault occurs.

The successful operation of a very large number of service restorers powered by manually wound or motor-wound torsional springs logically led to the investigation of a similar source.

From this investigation was developed a new and unique application of previously tried and proved principals. In a paper presented by A. E. Brock at the Pacific Coast convention in August 1941, an improved service restorer using a motor of an entirely new type for rewinding the operating spring was described.

This Vibratorque motor is adapted to this new application, but, instead of the customary secondary source of energy being provided, the motor is arranged to be directly in series with the primary line, thus making the new unit a completely self-contained device.

The operating energy of the service restorer is contained in a motor-rewound flat spiral spring where enough energy is stored to provide four opening operations and three reclosing operations on a sustained fault without rewinding, and an infinite number of operations on transient faults, as the Vibratorque motor restores the expended energy upon reclosing the circuit after each transient fault.

Figure 3 shows a schematic diagram of the entire mechanism.

Rating

This single-pole service restorer is rated at 15 kv 50 amperes continuous current-carrying capacity, with a maximum interrupting rating of 1,000 amperes when using the 25–50-ampere trip coils. When the lower current-rating trip coils are used, the interrupting rating is reduced to 40 times the rating of the trip coils installed.

The interrupting ability of this new service restorer has been amply proven by a comprehensive test program where currents of 1,490 amperes at 15 kv were interrupted on a full cycle of three reclosures and four openings.

A complete oscillographic record was made of the tests for record and study.

Two thousand operations were performed at moderate short-circuit currents considerably in excess of continuous current rating, on a 15-kv system, and, on being inspected, the service restorer was found in condition to perform many more operations.

A complete series of impulse tests was performed, and the results recorded on a cathode-ray oscillograph, to verify operation under lightning conditions, and in every case the restorer flashed over from bushing to ground outside the case.

Arc-Extinguishing Devices

The successful and co-ordinated performance of rural lightly loaded low-cost distribution lines under fault conditions depends primarily upon the rapid extinguishment of the arc and the high speed of the interrupting device protecting the circuit.

As the prestored spring operating energy is restrained by a roller-type latch,

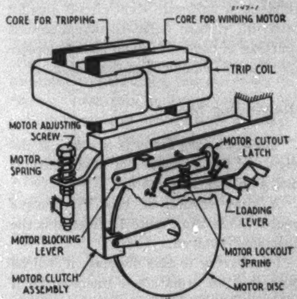


Figure 1. Diagram of Vibratorque motor

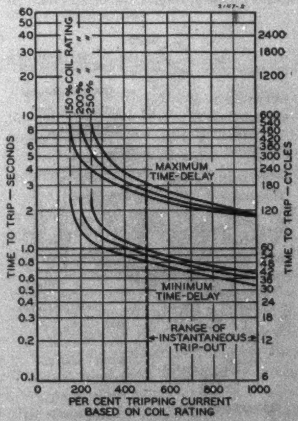


Figure 2. Time-current characteristic curves

Paper 42-147, recommended by the AIEE committee on protective devices for presentation at the AIEE Pacific Coast convention, Vancouver, British Columbia, Canada, September 9–11, 1942. Manuscript submitted June 1, 1942; made available for printing July 3, 1942.

E. E. Tugby is sales engineer, Pacific Electric Manufacturing Corporation, San Francisco, Calif.

---

with the gauge oil (that is, the oil outside the reservoir cells) must pass before entering the reservoir. It is felt that by removing the moisture in the air, less sludging will take place around the reservoir cells. The size of these cylinders is such that, with expected load cycles, the alumina should not have to be reactivated more often than once a year.

Load has been carried continuously since January 2, 1942. Operating data are being collected, such as duct temperatures, oil demands, cable movement at duct mouths, shifting of joints, and so forth. Sufficient information of this nature to draw conclusions will require considerable time to collect.

References

1. 120-Kv High-Pressure Gas-Filled Cable, I. T. Faucett, L. I. Komives, H. W. Collins, R. W. Atkinson. AIEE Transactions, volume 61, 1942, September section, pages 658–65.

2. 120-Kv Compression-Type Cable, I. T. Faucett, L. I. Komives, H. W. Collins, R. W. Atkinson. AIEE Transactions, volume 61, 1942, September section, pages 652–7.

3. Calculation of the Electrical Problems of Underground Cables, D. M. Simmons. *Electric Journal*, May–November, 1932.

4. Specifications for Impregnated-Paper-Insulated Lead-Covered Cable, "Oil-Filled" Type (third edition). Association of Edison Illuminating Companies, February 1938.

5. Thermal Transients and Oil Demands in Cables, F. O. Wollaston, K. W. Miller. AIEE Transactions, volume 52, 1933, pages 98–110.

Table I. Typical Interrupting-Capacity Tests

Test Item	Number of Tests	Amperes	Average Arcing Time (Cycles)	Duty
1	10	104	1.0	O—5 sec—*OCO—5 sec—OCO—9 sec—OCO
2	6	640	1.1	O—7 sec—OCO—7 sec—OCO—7 sec—OCO
3	6	1,459	0.99	O—7 sec—OCO—7 sec—OCO—7 sec—OCO

* OCO—Open, close, open.

and the fault current affects only the latching mechanism, the operating speed of the arc-interrupting device is independent of fault current. The moving blade travels with a high rate of speed to assure quick extinguishing of the arc in the expulsion chamber, and an actual test performed has shown arc time from 0.65 to 1.2 cycles of arc while interrupting approximately 1,500 amperes at 12.5 kv to ground. The blade tips of arc-resisting Elkonite also assure a minimum of material vaporization, thereby minimizing oil carbonization and consequently reducing maintenance and inspection.

A test run of in excess of 2,000 operations at a moderate short-circuit current and full voltage failed to reveal serious burning of contacts which gave complete proof of the adequacy of the design.

Rewinding Motor

The rewinding motor is of a unique design and has a number of novel features not available heretofore.

A cross section of this Vibratorque motor (shown in Figure 1) illustrates the principle involved.

In referring to this drawing, it will be seen that an armature is mounted on a spring that is rigidly supported at one end and has an adjustment to limit the travel of the spring at the other end. At right angles to the armature is an extension carrying a clutch that engages both faces of a flat hard steel disc. The clutch consists of two hardened steel-bearing rollers engaging in a tapered slot, so co-ordinated as to provide the most efficient transfer of energy from the vibrator to the steel disc, with the least friction on the return stroke. The armature is alternately attracted by an a-c magnet coil assembly consisting of two identical coils mounted on a laminated solenoid core, and returned by a spring. The reciprocal motion of the vibrator alternately causes the clutch to engage on the disc which travels forward a small increment, then to disengage and move back to its original position. The disc rotates in a rapid series of small increments which are transmitted through gears to the main spiral operating spring, where the energy is stored for use in operating the service restorer.

The speed of the output shaft is a function of the weight and size of the armature, ampere turns of the coil, and characteristics of the spring which supports the armature.

The Vibratorque motor develops compound characteristics and with a change in load assumes a moderate output speed change. The magnet coils which also act as trip coils are, as previously stated, connected in series with the line and arranged in two halves to give flexibility. Since they are in series with the line, no external source of operating energy is necessary, and, as the rewinding energy is provided during normal operating conditions, the stored energy is instantly available to operate the device during fault conditions.

The motor will operate successfully with the smallest coils when a current as low as one-half ampere flows through the line.

The magnet coils also perform a dual duty as trip coils and are available in ratings from 3–6 amperes to 25–50 amperes. It is to be noted that pairs of coils have a dual value; as, when the coils are connected in series, they have their lowest rating, that is, 3–6 ampere coils would carry three amperes, but if they were connected in parallel, they would have their higher rating of six amperes as a unit, with each coil carrying three amperes only.

When the Vibratorque motor has fully wound the operating spring to its required tension, the vibrator spring is automatically locked up against the pole pieces. This is controlled through a differential arrangement on the outer case surrounding the operating spring. A scroll is cut in this outer case, in which rides a stud projecting from a bar which thus travels across the face of the case and assumes a position with direct relation to the amount of spring operating energy available. With the spring fully wound, the projection is in a position near the outer periphery of the spring house, and the bar projects beyond the case. When this point is reached, a catch that is normally held under tension is released, and sufficient pressure is applied to the armature support spring to force it up against magnet coils and prevent further vibration. When the spring energy is expended, the differential bar moves down, under the action of the projection in scroll and re-engages the catch, which

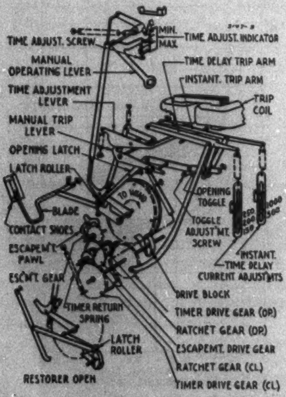

TIME ADJUST. SCREW
MIN.
MAX.
TIME ADJUST. INDICATOR
MANUAL OPERATING LEVER
TIME DELAY TRIP ARM
INSTANT. TRIP ARM
TIME ADJUSTMENT LEVER
TRIP COIL
MANUAL TRIP LEVER
OPENING LATCH
LATCH ROLLER
TO WIND
OPENING TOGGLE
BLADE
TOGGLE ADJUST'MT. SCREW
250
200
150
1000
500
CONTACT SHOES
ESCAPEM'T. PAWL
INSTANT.
TIME DELAY
CURRENT ADJUST'MTS
ESC'M'T. GEAR
DRIVE BLOCK
TIMER RETURN SPRING
TIMER DRIVE GEAR (OP.)
RATCHET GEAR (OP.)
LATCH ROLLER
ESCAPEMT. DRIVE GEAR
RATCHET GEAR (CL.)
RESTORER OPEN
TIMER DRIVE GEAR (CL)

Figure 3. Diagram of mechanism

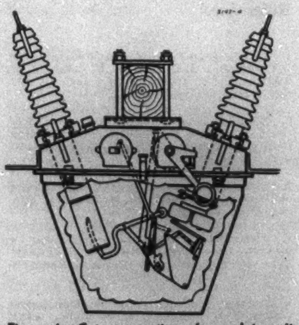


Figure 4. Cutaway section of complete unit

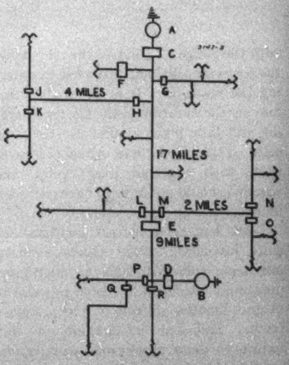

A
C
F
G
J
4 MILES
H
K
17 MILES
L
M
2 MILES
N
E
O
9MILES
P
D
Q
R
B

Figure 5. Typical sectionalized system

releases the spring to vibrate and again supply power to the operating spring.

The center of the coil spring is connected to a shaft which carries on its outer end an operating lever, which in turn is connected to the moving contact blade through a connecting rod.

This blade is normally held in the closed position by the operating spring, restrained by a roller latch mechanism. This type of latch mechanism has been found to give positive latching while requiring a minimum of current for tripping without any diminution of the tripping speed.

The latch may be released by one of the three following methods:

1. By the trip coils actuating the time-delay mechanism.
2. By the trip coils actuating the instantaneous high-current trip mechanism.
3. By the operation of the manual trip mechanism.

All three of these schemes act upon a single common toggle independently and, in the case of the first two, are independently adjustable over a wide range.

Escapement-Type Delay Mechanism

As the complete service restorer was designed for low-cost protection, it was imperative that all working parts be utilized to the fullest degree. Since both tripping and reclosing involved time delay, it was decided that one unit serving both operations could perform these duties admirably, and a mechanism was accordingly built with a rugged design that would handle both duties successfully and reliably.

This time-delay mechanism consists essentially of a gear train with an escapement pawl, which provides a time delay that is independent of temperature conditions.

The time-delay driving gear has a ratchet on either side, and each ratchet is connected to a smaller gear on a shaft. On one side this ratchet gear is driven by the opening gear which is connected to the opening toggle, and the other ratchet gear is actuated by the main blade through a suitable mechanism to control the reclosing time delay.

The correct initial starting position is always assured by a cam return spring that brings the gear train back to normal starting position. If the fault clears before the restorer opens, the time delay will immediately return to its initial position ready for a second fault, thus providing a high degree of accuracy of protection. The reclosing time delay is actuated by a projection on the main-blade operating lever. This projection initially engages the flat spring mounted on a cam which initiates the movement of the gear train. After a short travel, the main-blade operating lever engages the cam, and the driving effort of the main operating spring, transmitted through the main-blade operating lever, tends to rotate the cam, but rotation is restrained by the time-delay escapement.

The reclosing time schedule is fixed at seven seconds, but the tripping time delay may be varied. The tripping time is dependent upon the amount of travel of the escapement, which in turn may be limited by a time-adjustment lever. This lever adjusts the position of the trip arms relative to the trip-coil pole faces and is in its turn controlled through a linkage to the manual rewinding lever. This lever is adjustable to its final position by a set screw mounted external to the housing. Calibration points are provided to indicate the maximum and minimum adjustments, which may be easily adjusted in the field with a screw driver.

Manual Rewinding Mechanism

A manual rewinding mechanism is provided and so arranged that the restorer will not reclose until sufficient spring energy is stored for one closing and one opening operation, thus providing a valuable safety factor when closing in against a fault.

If there is no fault on the line, the rewinding motor then takes up its duties and completes the rewinding of the spring to its proper operating tension.

Time-Current Characteristics

Typical time-current characteristics are shown on Figure 2 where it may be noted that minimum trip is arranged for 150 per cent of rated coil current. That is, in the dual ratings with the coils of a 3–6-ampere combination connected in series for a three-ampere maximum current, the restorer would trip at 4.5 amperes, or if the trip coils were connected in parallel for six amperes continuous current, then the restorer would trip at nine amperes. The two sets of curves for minimum and maximum time delay represent the limits of the lever setting previously described, and each family of curves represents the current adjustment obtainable.

It should also be noted that at 500 per cent of rated coil current, an instantaneous trip mechanism comes into play, and the restorer may be tripped instantaneously. This minimum trip may be easily adjusted from 500 per cent to 1,000 per cent if a higher instantaneous trip value is desired. The current adjustments are both internal to the restorer and may be made with a screw driver as calibration marks are provided, and an indicator may be set for each adjustment.

Mounting

This new restorer is designed for either single-arm or direct-pole mounting and may be readily arranged for either type mounting as required.

Construction

Referring to Figure 4, it will be seen that this new single-pole service restorer consists essentially of a pivoted blade actuated by a small coil spring, mounted on the lower end of the one bushing and an expulsion contact mounted on the lower end of the other bushing. Both bushings are mounted, diverging outwardly in the cover, and are of the standard stud type. A slow-speed motor rewinds the operating spring after each reclosure, and this makes available an infinite number of reclosures on transient faults. On a sustained fault, the prestored spring energy is dissipated at such a rate that, after the fourth spring operation following three reclosures, the restorer will not reclose and is "locked out." To reclose the restorer after lockout, it is necessary to manually rewind with the manual rewinding handle for four or five strokes, to store enough energy for one closing and one opening operation before the restorer closes and the motor begins its rewinding cycle.

Application

The problem of providing high-quality protection against faults, and the maintenance of a reasonable continuity of service at a cost comparable with the revenue obtainable with long lightly loaded low-revenue lines have been brought to the fore more rapidly and forcefully with the shift of population from the cities to the country.

The availability of modern low-cost electric appliances in these rural areas has further heightened the pressure to maintain high-quality service, and, inasmuch as the average rural circuit is of the radial feeder type, it is of utmost importance that faults be localized as much as possible and confined to the least number of customers.

Due to the nature of the territory in which these rural lines are located and the types of low-cost construction used, the prevalence of faults due to lightning disturbances and to trees swinging lines together in wind storms and whipping under sleet conditions is very high.

Studies made in many parts of the country show that an average of 80 per cent of faults may be cleared, and service restored on a first reclosure, which thus justifies the use of reclosing devices on a rural system.

The development of the three-pole service restorer described at the AIEE 1941 Pacific Coast convention was one answer to the problem of high-quality protection for the main feeder, and the development of the single-pole unit described in this paper complements the three-pole unit to relieve the three-phase service restorers of a large number of operations, and to localize a sustained fault to any one particular feeder.

An interesting application is illustrated in Figure 5 based on a co-operative system with widely scattered individual customers and on a moderately heavy load. *A* and *B* are two interconnected generating stations with three-pole motor-rewound service restorers at *C*, *D*, and *E* in the three-phase interconnecting power lines, and at *F* to protect a large load. At restorer *E* potential and current transformers were installed in the main line, and power directional relays were provided to prevent generating station *B* from carrying the entire load if generating station *A* were out of service. Single-pole restorer *G* protects one branch line that is in a reasonably clear area and would not be subject to serious faults. Single-pole restorers *J* and *K* protect long lines radiating through heavily wooded area and are in cascade with a single-pole restorer at *H*. Similarly, single-pole restorer *M* also is in cascade with restorers *N* and *O* to protect another area in trees. Restorers *P*, *Q*, and *R* protect feeders in the vicinity of generating station *B*. As no fuses are to be used for sectionalizing protection on this system, the problem of co-ordination was simply one of superimposing the time-current characteristic curves of single-pole service restorers on those of the three-pole service restorer and arranging tripping time and current settings so as to provide a high degree of selectivity.

At each distribution transformer installation, individual fuse protection was provided; therefore, the problem resolved itself into a group of individual circuit calculations to balance all factors to obtain the greatest flexibility, at the same time restricting outages to the smallest area.

A check was made of the connected capacity on each branch feeder, and it was found that, in any individual leg to be protected, the connected transformer capacity did not exceed 20 kva. On the basis of a load factor of 50 per cent the load current would not exceed 1.5 amperes per circuit, which was well under the continuous current-carrying capacity of the smallest trip coil available.

Therefore all single-pole service restorers used were to be equipped with the same size trip coil, namely 3–6 amperes, and were arranged where there were two in cascade so that the unit nearest the source had the trip coils connected in multiple, for six amperes continuous current-carrying capacity, and those furthest out were arranged with the coils in series for three amperes continuous current-carrying capacity. It was then necessary to make field adjustments as to the time delay and instantaneous trip at various points to properly co-ordinate the entire system.

The three-pole service restorers *C* and *D* at the generating station were each equipped with a first instantaneous tripping action, followed by time-delay tripping on subsequent tripping operations.

It is thus to be seen that each branch line is fully protected by a single-pole restorer which will trip after a time delay. The main-line protective restorer at the generating stations, being equipped with instantaneous initial trip and instantaneous first reclosure, will operate to protect the entire system against heavy transient faults in the main feeders.

The single-pole restorers would be arranged so that the tripping time delay is less than that of the main-line restorer on the second trip out; thus under sustained fault conditions the single-pole restorer will take over the duty of isolating the fault, and the remainder of the system continues with uninterrupted service. Under transient fault conditions the single-phase restorers will clear a fault on the branch lines, of lesser magnitude than that required to operate the main-line restorer at either *C* or *D* and will automatically restore the expended operating energy upon the line circuit being re-energized.

In the case of the system under discussion, after three transient faults had been cleared, each subsequent operation of the single-pole service restorer may be considered as saving the expense of a service man with his transportation to and from the maintenance base to patrol the line and restore service. This saving of labor and transportation offers a considerable reduction in maintenance expense and at the same time provides a very high quality of service in rural areas, comparable to that obtainable in suburban areas, at a very low initial cost.

SUPPLEMENT TO
ELECTRICAL ENGINEERING
TRANSACTIONS SECTION

PUBLISHED BY THE
AMERICAN INSTITUTE OF
ELECTRICAL ENGINEERS

DECEMBER 1942

Supplement to Electrical Engineering—Transactions Section

December 1942

This Supplement comprises pages 893–1076, inclusive, of the 1942 AIEE Transactions, volume 61, published in this form for the convenience of members who do not subscribe to Transactions. It contains: (1) those technical papers presented at the 1942 summer convention and not previously published in the six monthly Transactions sections of Electrical Engineering, July to December 1942, inclusive; (2) one late 1942 winter convention discussion and closure; (3) all approved discussions on 1942 North Eastern District meeting papers, 1942 summer convention papers, and 1942 Pacific Coast convention papers.

1. TECHNICAL PAPERS

Pages

Stability of Transmission Systems 893
John Grzybowski Holm

Emergency Overloading of Power Transformers 906
V. M. Montsinger, P. M. Ketchum

Induced Voltages on Transmission Lines 916
C. F. Wagner, G. D. McCann

Load Ratings of Cable—II 930
Herman Halperin

Behavior of Directional Relays 942
Troy D. Graybeal

Combination of Probability Curves 953
Roger I. Wilkinson

Series Capacitors for Transmission Circuits 963
E. C. Starr, R. D. Evans

2. DISCUSSIONS

Basic Sciences

Pages

Reactance and Skin Effect of Conductors 976
Herbert B. Dwight

***Vi* Energy-Flow Postulate** 1054
Joseph Slepian

History of A-C Wave Form 1071
Frederick Bedell

Communication

Regulated Rectifiers in Telephone Offices 985
D. E. Trucksess

Electrochemistry

Electric Equipment for Electrochemical Installation 1062
T. R. Rhea, H. H. Zielinski

Electronics

Pages

Sealed-Tube Ignitron Rectifiers 1052
M. M. Morack, H. C. Steiner

Ignitor Excitation Circuits 1061
A. H. Mittag, A. Schmidt, Jr.

Electric Machinery

Rectifier Terminology and Circuit Analysis 974
C. H. Willis, C. C. Herskind

Temperature-Aging Tests on Motor Stators 977
J. A. Scott, B. H. Thompson

Emergency Overloads for Oil-Insulated Transformers 988
F. J. Vogel, T. K. Sloat

Mechanical Deterioration of Cellulose Insulation 991
F. M. Clark

Emergency Overloading of Power Transformers 993
V. M. Montsinger, P. M. Ketchum

(Concluded on inside back cover)

Contents copyrighted 1942 by the American Institute of Electrical Engineers, 33 West 39th Street, New York, N. Y. Printed in the United States of America

Stability Study of A-C Power-Transmission Systems

JOHN GRZYBOWSKI HOLM
MEMBER AIEE

Part I

FOR the operation of power-transmission systems of medium and long length, the problem of stability is becoming increasingly important. This is true in the steady state of the system as well as in the transient state, during and after the occurrence of a fault on the system. Since the design of the transmission system is primarily responsible for its proper operation, and since the a-c network analyzer offers the best means for system analysis, the present study was undertaken for the purpose of correlating the design and operational data by means of the a-c network analyzer. For this purpose several power-transmission systems were selected, and their steady-state- and transient-stability characteristics studied. Thus the steady-state- and transient-stability limits of the system were obtained on the analyzer.

As a result of the study, curves were constructed from which it is possible to determine, at various voltages, the stability limits of transmission systems of various lengths, to select the proper voltage for a certain transmission project, and to determine the kilovolt-ampere capacity of generators to be installed, in order that certain steady-state- or transient-stability limits might be attained. Curves were suggested for rapid estimating of system steady-state- and transient-stability limits. Certain means for increasing the stability limits were studied, such as the intermediate synchronous condenser, the resistance in the neutral of the sending transformer, and the reactor on the generator bus. The necessity of the application of these means to the various systems, and the need for generators of special design with lower-than-normal transient reactance and higher inertia constant are shown.

General Principles of System Design

Thirty-one power-transmission systems were designed for the purpose of purely academic study. The systems transmit blocks of power of 50,000, 100,000, 250,000, 500,000, and 800,000 kw (at the receiving end) over distances of 50, 75, 150, 250, and 500 miles, at sending-end voltages of 69, 138, 230, and 345 kv, at 60-cycle frequency. The power is transmitted from hydroelectric power plants to distant metropolitan load centers, and the transmission lines were designed so as to transmit the predetermined block of power. In line with the trend toward high standards of reliability and security of operation, the systems were required to deliver a 100 per cent block of power under a double line-to-ground fault at the sending-end bus, cleared in 10.5 cycles with a consequent loss of a circuit, or a section of it if the circuits are sectionalized. Each system thus has a minimum of two circuits. The block of power transmitted under these conditions and delivered at a corona power loss as small as is economically justified, is taken as the rating of the system.

The long-line theory of line design was used throughout, and the general circuit constants were calculated for the line alone, for a circuit, or for a section of it. In several cases the transmission of a certain block of power over a certain distance was made at two different voltages in order that the effect of the voltage on system stability could be shown.

Transmission-Line Data

All systems were designed to operate at standard voltages at both ends of the system. The voltage drop of approximately five per cent in the line was maintained by means of synchronous condensers at the receiving end of the system.

On all systems under study, only copper conductors were used—both the standard concentric stranded conductor and the hollow copper conductor of the *HH* type manufactured by the General Cable Corporation. The corona power loss was calculated by the Carroll-Rockwell method.[1–3] In view of the great difference in the corona power loss of the two types of conductors, it became necessary to establish conditions for the use of either of the two conductors. A rather conservative criterion was established by which the hollow-type conductor was used whenever the cross section of the concentric stranded copper conductor, of the diameter necessary to secure the desired corona power loss, was about 50 per cent greater than the cross section required of this type of conductor to carry the emergency current of the line.

In every other respect the lines are of conventional design.[4–6] In view, however, of the effect that certain design factors have on system stability limits, it is pointed out that all systems have high-voltage busses at both ends, and all are equipped with a single continuous-type tower-to-tower counterpoise. The list of the systems designed, the types and sizes of conductors, and the corona power losses are given in Table I. Normal and emergency circuit conditions of the transmission lines are represented in Figure 1.

System Characteristics and Representation on the A-C Network Analyzer

The basic study of systems on the network analyzer was made for those with generators of standard design, and not equipped with devices for stability improvement, in order that the stability limits might be obtained for systems with standard equipment. Subsequently the various means for improvement of stability were investigated (see part II) and applied where needed.

The generators were not equipped with damper windings for this study, and their saturated synchronous reactance was taken at 80 per cent of the unsaturated direct-axis synchronous reactance assumed to be 100 per cent. The transient and negative-sequence reactances were taken at 35 and 45 per cent, respectively. The transformer reactances were taken at standard values, with 12.5 per cent for the 345-kv units. For the basic study, the high-voltage neutral of transformers was solidly grounded in all systems. The saturated synchronous reactance of the receiving-end synchronous condensers was taken at 128 per cent, and the transient and negative-sequence reactances were taken at 40 and 24 per cent, respectively.

The transmission systems feed into a metropolitan-system load. The magnitude of this load is assumed to vary in-

---

Paper 42-117, recommended by the AIEE committee on power transmission and distribution for presentation at the AIEE summer convention, Chicago, Ill., June 22–26, 1942. Manuscript submitted November 21, 1942; made available for printing May 19, 1942.

JOHN GRZYBOWSKI HOLM is with the Stone and Webster Engineering Corporation, Boston, Mass.

To the engineers of the General Electric Company, the Westinghouse Electric and Manufacturing Company, the General Cable Corporation, and The Okonite Company, and to several electric-public-utility and consulting engineers, the author expresses appreciation for valuable advice and data.

versely with the block of power transmitted, ranging from 400,000 kw for the 50,000-kw systems to 1,600,000 kw for the 800,000-kw systems. The power factor of the load is 0.85 lagging. In the circuit representation for the network-analyzer study, the metropolitan load was represented by an equivalent synchronous motor with proper constants and excitation voltage. Such a representation follows from the study of the input characteristics to the metropolitan-load system which, when plotted in the form of synchronous motor charts, resemble the characteristics of the synchronous-motor charts.[7] The reactance of the equivalent synchronous-motor load was taken at the same value for both the steady-state- and the transient-stability studies. Similarly the positive- and the negative-sequence reactances of the load were taken equal.

The inertia constant of generators and synchronous condensers was taken at standard values for the sizes of machines assumed.[8] The inertia constant of the load was calculated for the proportion of turbogenerators and synchronous motors known to be representative of large metropolitan-load centers, and ranged from about 5.15 to 5.60 kw-sec per kva.

System reactances, as represented on the a-c network analyzer, are given in Table II.

Synchronous Condensers at the Receiving End of Systems

Synchronous condensers were placed at the receiving end of every system, of sufficient capacity to maintain rated voltage under all conditions from full load to no load, at an approximate ratio of 1.05 of the voltages at the system ends. The leading kilovolt-amperes were determined for the 100 per cent load under emergency conditions. At the 1.0 to 0.5 lead-to-lag ratio of this condenser, the lagging kilovolt-amperes measured at no load and under normal circuit conditions, were always below the maximum allowed by this ratio.

Table I. List of Systems Designed—Type and Size of Conductors—Corona Power Loss

System Identification Number	Block of Power Transmitted to the Receiving End (Kw_r)	Transmission Distance (Miles)	Transmission Voltage at Sending End (Kv_s)	Number of Circuits in System	Conductor: Type	Conductor: Cross Section (Circular Mils)	Conductor: Outside Diameter (Inch)	Corona Power Loss Three-Phase Loss Per Mile of One Circuit (Kw)
0-1*a*	50,000	50	69	2	Concentric stranded copper	250,000	0.574	0.52
0-2*a*	50,000	50	138	2	Hollow, *H H*-type (*UM*)	138,000	0.600	0.89
0-3*a*	50,000	75	69	2	Concentric stranded copper	250,000	0.679	0.40
0-4*a*	50,000	75	138	2	Hollow, *H H*-type (*UM*)	138,000	0.600	0.89
0-5*a*	50,000	150	138	2	Concentric stranded copper	250,000	0.574	1.63
0-6*a*	50,000	250	138	2	Concentric stranded copper	300,000	0.628	1.30
0-7*a*	100,000	50	138	2	Hollow *H H*-type	212,000	0.600	0.89
0-8*a*	100,000	75	138	2	Concentric stranded copper	250,000	0.574	1.63
0-9*a*	100,000	150	138	2	Concentric stranded copper	400,000	0.725	0.865
0-10*a*	100,000	150	230	2	Hollow, *H H*-type (*UM*)	265,000	0.950	0.75
0-11*a*	100,000	250	230	2	Hollow, *H H*-type (*UM*)	265,000	0.950	0.75
0-12*a*	250,000	50	138	2	Concentric stranded copper	850,000	1.062	0.36
0-13*a*	250,000	50	230	2	Hollow, *H H*-type (*NM*)	316,000	0.950	0.75
0-14*a*	250,000	75	230	2	Hollow, *H H*-type (*NM*)	316,000	0.950	0.75
0-15*a*	250,000	150	230	2	Hollow, *H H*-type	400,000	0.950	0.75
0-16*a*	250,000	250	230	2	Concentric stranded copper	650,000	0.929	2.07
0-17*a*	250,000	250	345	2	Hollow, *H H*-type (*UM*)	505,000	1.400	0.61
0-18*a*	250,000	500	345	2	Hollow, *H H*-type (*NM*)	587,000	1.400	0.61
0-19*a*	500,000	50	230	3	Hollow, *H H*-type (*NM*)	316,000	0.950	0.75
0-20*a*	500,000	75	230	3	Hollow, *H H*-type (*NM*)	316,000	0.950	0.75
0-22*a*	500,000	150	230	3	Concentric stranded copper	700,000	0.964	1.72
0-23*a*	500,000	150	345	2	Hollow, *H H*-type (*UM*)	505,000	1.400	0.61
0-24*a*	500,000	250	345	2	Hollow, *H H*-type (*UM*)	505,000	1.400	0.61
0-25*a*	500,000	500	345	3	Hollow, *H H*-type	650,000	1.400	0.61
0-26*a*	800,000	50	230	3	Concentric stranded copper	750,000	0.998	1.44
0-27*a*	800,000	50	345	3	Hollow, *H H*-type (*UM*)	505,000	1.400	0.61
0-28*a*	800,000	75	230	3	Concentric stranded copper	800,000	1.031	1.22
0-29*a*	800,000	75	345	3	Hollow, *H H*-type (*UM*)	505,000	1.400	0.61
0-30*a*	800,000	150	345	3	Hollow, *H H*-type (*UM*)	505,000	1.400	0.61
0-31*a*	800,000	250	345	3	Hollow, *H H*-type (*UM*)	505,000	1.400	0.61
0-32*a*	800,000	500	345	4	Hollow, *H H*-type	900,000	1.400	0.61

The system identification number is kept throughout the entire study.

Hollow, *H H*-type copper conductor, General Cable Corporation patents.
Hollow, *H H*-type—standard size.
Hollow, *H H*-type (*NM*)—normal minimum size.
Hollow, *H H*-type (*UM*)—undercut minimum size.

Corona power loss calculated for an elevation of 3,300 feet above sea level and a temperature of 50 degrees Fahrenheit. The equivalent-delta spacings are 10.28 feet for 69-kv lines, 16.67 feet for 138-kv lines, 32.13 feet for 230-kv lines, and 47.88 feet for 345-kv lines.

The kilovolt-amperes of the receiving-end condensers, as obtained from measurements on the a-c network analyzer, are given in Table III. The average leading kilovolt-amperes per kilowatt of the line rating is 1.05 kva in the 50- to 150-mile lines. In the 250- and 500-mile systems, equipped with intermediate synchronous condensers, the average is 0.85 kva per kilowatt of system rating.

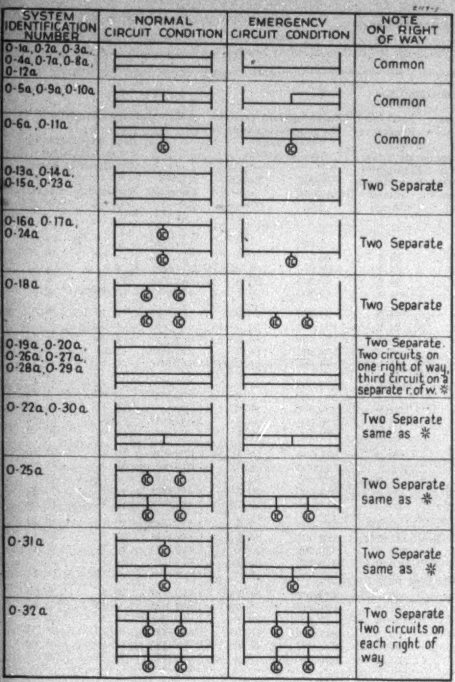
The analysis of the figures obtained substantiates the common experience, to the effect that the receiving-end condenser kilovolt-amperes required per transmitted kilowatt increase as the transmission distance or the block of transmitted power increases, and decrease with an increase of the transmission voltage, the other parameters remaining unchanged. The data which have been obtained for a large variety of systems make it possible to estimate the required kilovolt-amperes of receiving-end synchronous condensers for numerous systems, either by direct use of the figures given or by their judicious interpolation.

It should be borne in mind, however, that the systems designed do not make use of the rotating equipment within the metropolitan-load area into which they feed, and are thus made independent of it. Should such use be made, the condenser kilovolt-amperes may be reduced.

The receiving-end condenser unquestionably contributes somewhat toward the steady-state- and transient-stability limits of the system, and hence toward the system rating. The degree of this contribution, however, is difficult to determine rigorously (if, indeed, it can be determined at all), in view of the difficulty in maintaining the same voltages at the ends of the system in the two cases where the system has, and where it does not have, a condenser at its receiving end.

Procedure and Approximations in the Stability Study

The determination of the steady-state- and transient-stability limits was made according to the standard accepted procedure.[9] The steady-state-stability study was made for normal circuit conditions of the system. From the stability limit obtained on the network analyzer, the steady-state-stability limit is calculated for emergency circuit conditions. This calculation is made on the basis that the steady-state limit changes inversely with the total system reactance. The transient-stability limit was determined by the point-by-point method for the condition of a double line-to-ground fault near the sending-end bus. The negative-sequence-



SYSTEM IDENTIFICATION NUMBER	NORMAL CIRCUIT CONDITION	EMERGENCY CIRCUIT CONDITION	NOTE ON RIGHT OF WAY
0-1a, 0-2a, 0-3a, 0-4a, 0-7a, 0-8a, 0-12a			Common
0-5a, 0-9a, 0-10a			Common
0-6a, 0-11a			Common
0-13a, 0-14a, 0-15a, 0-23a			Two Separate
0-16a, 0-17a, 0-24a			Two Separate
0-18a			Two Separate
0-19a, 0-20a, 0-26a, 0-27a, 0-28a, 0-29a			Two Separate. Two circuits on one right of way, third circuit on a separate r. of w. ✳
0-22a, 0-30a			Two Separate same as ✳
0-25a			Two Separate same as ✳
0-31a			Two Separate same as ✳
0-32a			Two Separate Two circuits on each right of way

Figure 1. Normal and emergency circuit conditions of transmission lines (single-line diagrams)

IC indicates an intermediate synchronous condenser. Receiving-end synchronous condensers not shown. For commentary of identification numbers of systems, see Table I

and zero-sequence-impedance networks of the system were formed as seen from the place of fault.

Regular simplifications were introduced for the system study on the network analyzer. Of these the following are the most important: The saliency of machines was not accounted for, exciter action was not introduced, and only the first swing of machines was considered. In this part of the paper the stability limits of the 50- to 150-mile systems are reported as obtained in the basic study on the network analyzer, that is, for systems of standard equipment with no devices for stability improvement. The basic study of the 250-mile and 500-mile systems shows that it is absolutely impractical to operate lines of this length with standard equipment, when no devices of any kind are employed for the increase of stability limits. In the present study the intermediate synchronous condenser (see part II) was adopted as the basic device necessary for the satisfactory operation of the 250-mile and 500-mile systems. Therefore, the stability limits of these systems are reported in this part of the paper as obtained for systems equipped with intermediate synchronous condensers, with machines of standard design, and otherwise with standard equipment. The additional means which might be necessary to bring the transient-stability limit of the system to the desired level are discussed in part II.

Discussion of the Steady-State-Stability Limit

The steady-state-stability limit under both normal and emergency circuit conditions is given in Table IV and for normal circuit conditions is represented in Figure 2 as a function of the length of the transmission line. Similarly the stability limit may be represented as a function of the sending-end voltage or of the kilovolt-ampere capacity of installed generators.

The family of curves obtained is susceptible to interpolation and may be used for the following purposes:

1. To determine the steady-state-stability limit of the system when the line length, the system voltage, and the installed generator capacity are known.

2. To select an appropriate transmission voltage for a certain project when the design is to be based on the magnitude of the steady-state-stability limit.

3. To determine the kilovolt-ampere generator capacity which must be installed in a system of a certain length operating at a certain voltage, if a certain steady-state-stability limit is desired. Also to determine the maximum transmission distance when the generator kilovolt-ampere capacity and the system voltage are known.

4. To give an indication of conditions where the steady-state-stability limit may be within the economical region, and hence an indication of the order of magnitude of the kilovolt-ampere generator capacity which can be economically installed.

Figures on the steady-state stability obtained on the network analyzer on a wide variety of systems clearly demonstrate the relative effect on the stability limit of the system reactance and the transmission voltage. In the heavy lines the large kilovolt-ampere generator capacity has an effect on the steady-state-stability limit which is greater than that exerted by the transmission voltage. Since the systems designed represent fairly economical combinations of the block of power transmitted and the transmission distance, it can be seen that in the longest systems the steady-state-stability limit is more affected by changes in the mileage than by changes in the system voltage. This again has a bearing on the economics of the system.

Curves for Estimating the Steady-State-Stability Limit

Further correlation of data obtained on the a-c network analyzer makes it possible to express the steady-state-stability limit as a function of a quantity which for convenience is denoted as β, thus providing a family of curves for quick estimation of the stability limit of the system. The interpretation is simple and provides a sufficient degree of accuracy in estimating the stability limit of a great variety of trunk-line systems differing in details from those investigated.

The stability limit is plotted as a function of the quantity

$$\beta = \frac{E_s \cdot E_r}{X} \qquad (1)$$

where E_s and E_r are, respectively, the sending- and receiving-end line-to-line voltages in kilovolts, and X is the total system reactance as taken for the steady-state calculations and expressed in ohms on the system generator base. More specifically, X is the sum of the generator saturated synchronous reactance, the reactances of the sending-end transformer, the transmission line under either normal or emergency conditions as the case might be, the receiving-end transformer, and the load reactance as taken for the steady-state study.

Table II. System Reactances

(As Represented on the A-C Network Analyzer for System-Stability Study)

	Generators (Waterwheel)			Sending-End Transformers Delta-Grounded-Wye		Transmission Line		Receiving-End Transformers Grounded-Wye-Delta-Grounded-Wye		Metropolitan-System Load		
											Load Representation as Equivalent Synchronous Motor	
System Identification Number	Total System Kva	Synchronous Reactance X_s'—Saturated (Ohms)	Transient Reactance X_d' (Ohms)	Total System Kva	Reactance (Ohms)	Total Line Reactance of All Circuits Under Normal Circuit Conditions (Ohms)	Total Line Reactance of All Circuits Under Emergency Circuit Conditions (Ohms)	Total System Kva	Reactance (Ohms)	Load Assumed (Kva)	Resistance (Ohms)	Reactance (Ohms)
0-1*a*	55,200	69.0	30.2	55,200	6.05	19.3	38.6	53,500	5.7	471,000	3.1	4.65
0-2*a*	52,400	291.0	127.0	52,400	30.9	19.85	39.7	50,800	29.1	471,000	12.35	18.5
0-3*a*	57,700	66.1	28.9	57,700	5.8	27.85	55.7	57,200	5.35	471,000	3.1	4.65
0-4*a*	53,300	286.0	125.0	53,300	30.4	29.8	59.6	51,900	28.5	471,000	12.35	18.5
0-5*a*	53,500	285.0	124.6	53,500	30.3	61.8	91.5	50,800	29.1	471,000	12.35	18.5
0-6*a*	54,700	279.0	122.0	54,700	29.6	100.8	151.0	51,300	28.85	471,000	12.35	18.5
0-7*a*	105,800	144.2	63.0	105,800	15.3	19.9	39.8	102,600	14.4	647,000	8.95	13.45
0-8*a*	107,400	142.0	62.0	107,400	15.1	31.0	62.0	104,900	14.2	647,000	8.95	13.45
0-9*a*	110,100	138.5	60.6	110,100	14.7	59.7	88.5	103,800	14.4	647,000	8.95	13.45
0-10*a*	106,100	400.0	175.0	106,100	54.9	61.6	91.25	103,800	51.3	647,000	24.9	37.4
0-11*a*	110,000	386.0	168.7	110,000	53.0	99.1	148.65	103,800	51.3	647,000	24.9	37.4
0-12*a*	270,000	56.4	24.7	270,000	6.0	18.7	37.4	264,600	5.6	1,030,000	5.65	8.45
0-13*a*	260,200	162.5	71.0	260,200	22.3	20.6	41.2	254,000	20.9	1,030,000	15.65	23.45
0-14*a*	266,000	159.4	69.6	266,000	21.9	30.6	61.2	256,500	20.8	1,030,000	15.65	23.45
0-15*a*	287,000	147.8	64.5	287,000	20.3	60.5	121.0	279,000	19.1	1,030,000	15.65	23.45
0-16*a*	274,000	154.5	67.5	274,000	21.2	104.5	209.0	264,600	20.1	1,030,000	15.65	23.45
0-17*a*	267,200	356.0	156.0	267,200	55.6	101.1	202.2	256,500	53.0	1,030,000	35.25	53.0
0-18*a*	277,000	345.0	151.0	277,000	53.8	200.5	401.0	256,500	53.0	1,030,000	35.25	53.0
0-19*a*	532,000	79.3	34.7	532,000	10.9	13.75	20.6	508,000	10.5	1,470,000	10.95	16.45
0-20*a*	541,500	78.0	34.1	541,500	10.7	20.45	30.6	513,000	10.35	1,470,000	10.95	16.45
0-22*a*	570,500	74.0	32.4	570,500	10.2	41.5	62.3	553,000	9.6	1,470,000	10.95	16.45
0-23*a*	541,000	176.5	77.2	541,000	27.55	60.5	121.0	529,000	25.7	1,470,000	24.7	37.1
0-24*a*	557,500	171.0	74.7	557,500	26.65	101.3	202.6	529,000	25.7	1,470,000	24.7	37.1
0-25*a*	547,500	174.0	76.1	547,500	27.2	133.2	200.0	518,500	26.2	1,470,000	24.7	37.1
0-26*a*	842,000	50.4	22.1	842,000	6.9	13.9	20.9	821,000	6.5	1,882,000	8.55	12.85
0-27*a*	833,000	114.5	30.0	833,000	17.85	13.8	20.75	812,500	16.75	1,882,000	19.3	28.95
0-28*a*	889,000	47.6	20.85	889,000	6.55	20.9	31.3	838,000	6.35	1,882,000	8.55	12.85
0-29*a*	840,000	113.4	49.5	840,000	17.7	20.4	30.6	812,500	16.75	1,882,000	19.3	28.95
0-30*a*	868,000	110.0	48.0	868,000	17.15	40.3	60.5	821,000	16.55	1,882,000	19.3	28.95
0-31*a*	870,000	110.0	47.9	870,000	17.1	67.4	101.2	829,000	16.4	1,882,000	19.3	28.95
0-32*a*	885,000	107.6	47.0	885,000	16.8	100.65	134.2	829,000	16.4	1,882,000	19.3	28.95

Reactances are calculated on the system base and are represented on the high-voltage side.

The curves are represented in Figure 3 for normal circuit conditions of the transmission line. Separate lines are drawn for the systems equipped with intermediate synchronous condensers.

While it is true that the kilovolt-ampere generator capacity has an effect on the system stability limit, this does not preclude the possibility of using the curves for transmission systems whose relative generator and line reactances are different from those which served as a basis for the β curves. The curves are applicable to any fairly economical combinations of kilowatts, mileages, and voltages, and may be used with a degree of accuracy well within normal engineering estimates. They cannot be expected to give reliable figures (if, indeed, any can be given) in obviously uneconomical and mpractical cases such as the transmission of 50,000 kw over a distance of 500 miles at a voltage of 345 kv, or the transmission of 800,000 kw over 250 miles at 138 kv.

The metropolitan-system load of the transmission line for which the β curves are used has unquestionably an effect on the system-stability limit, but, as may be seen from the systems investigated, the load reactance constitutes a relatively small percentage of the total reactance X in the equation 1. Considering that the load on the systems designed was carefully determined with a view to representing actual cases as nearly as possible, it may be said that the method of proposed β curves may be used with the customary degree of engineering accuracy for systems having loads such as are encountered in actual practice. The gaps between the separate β curves could have been filled, had a greater variety of cases been investigated, or had other voltages been used. In the expression for β, excitation voltages could have been made use of instead of the sending- and receiving-end voltages. This, however, would introduce a great many additional calculations which are not justified by the ultimate accuracy possible of attainment.

Discussion of the Transient-Stability Limit

In the process of determining the transient-stability limit on the a-c network analyzer, it was found that the transient-stability limit of a system B may be calculated to a high degree of accuracy from the known transient-stability limit of a system A similar to it in configuration, by using the following expression:

$$(\text{Transient-stability limit})_B = \frac{P_A}{P_B} \cdot \frac{X_A}{X_B} \cdot \left[\frac{E_B}{E_A}\right]^2 \cdot (\text{transient-stability limit})_A \quad (2)$$

where P is the system rating determined solely from consideration of the conductor size and installed transformer capacity, X is the total system transient reactance of the emergency circuit, and E is the nominal system voltage. The subscripts refer to system A or B. More specifically, X is the sum of the generator transient reactance, the reactances of the sending-end transformers, the transmission line under emergency circuit conditions, the receiving-end transformers, and the load. Equation 2 gives a good check with the data obtained by actual measurement on the analyzer, the error not exceeding five per cent. In applying it, the intermediate condensers, if any, may be disregarded. This is explained by the comparatively small increase in the transient-stability limit caused by these condensers (see

Table III. Kva of Synchronous Condensers at the Receiving End of Systems as Determined on the A-C Network Analyzer

System Identification Number	Total Condenser Kva.	Condenser Kva Per Kilowatt of Line Rating, Kva/Kw.	Condenser Kva Per Kilowatt of Steady-State-Stability Limit Under Normal Circuit Conditions, Kva/Kw of SSSL-N	Remarks
0-1*a*	58,700	1.17	0.61	
0-2*a*	42,750	0.86	0.39	
0-3*a*	67,600	1.35	0.83	
0-4*a*	50,900	1.02	0.51	
0-5*a*	37,750	0.76	0.49	
0-6*a*	32,100	0.64	0.44	One *IC* at midpoint
0-7*a*	95,600	0.96	0.49	
0-8*a*	110,000	1.10	0.60	
0-9*a*	105,900	1.06	0.79	
0-10*a*	54,900	0.55	0.34	
0-11*a*	61,100	0.61	0.37	One *IC* at midpoint
0-12*a*	298,000	1.18	0.71	
0-13*a*	235,000	0.94	0.51	
0-14*a*	253,000	1.01	0.61	
0-15*a*	352,000	1.41	1.09	
0-16*a*	285,000	1.14	0.97	One *IC* at midpoint
0-17*a*	203,000	0.81	0.60	One *IC* at midpoint
0-18*a*	214,000	0.86	0.70	Two *IC* equally spaced
0-19*a*	485,000	0.97	0.58	
0-20*a*	526,000	1.05	0.70	
0-22*a*	688,000	1.38	1.17	
0-23*a*	639,000	1.28	1.00	
0-24*a*	537,000	1.07	0.91	One *IC* at midpoint
0-25*a*	410,000	0.82	0.84	Two *IC* equally spaced
0-26*a*	817,000	1.02	0.68	
0-27*a*	688,000	0.86	0.52	
0-28*a*	960,000	1.20	0.82	
0-29*a*	735,000	0.92	0.63	
0-30*a*	870,000	1.09	0.91	
0-31*a*	772,000	0.96	0.88	One *IC* at midpoint
0-32*a*	602,500	0.75	0.84	Two *IC* equally spaced

For commentary on system identification numbers, see Table I.

Approximate sending- and receiving-end voltage ratio 1.05.

Condenser kilovolt-amperes given leading.

IC denotes intermediate synchronous condenser placed for 100 per cent load.

part II of paper), the increase itself being counterbalanced by the error inherent in equation 2. Thus, by a judicious use of this expression, a saving of time may be effected in estimating the transient-stability limit of one system through the known stability limit of another similar system.

The transient-stability limit of the designed systems is given in Table IV. The table also gives the stability limit for zero-duration time of a double line-to-ground fault. This calculation is made by using the appropriate curve of Figure 11 of the First Report of Power-System Stability.[8] The latter curve represents a long and heavy line, and it has been checked on the writer's systems, resulting in data confirming it.

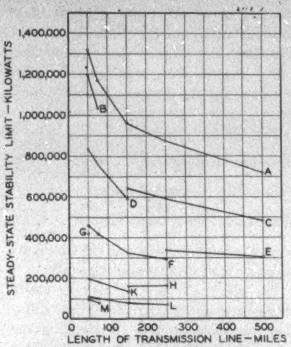


Figure 2. Steady-state-stability limit

Normal circuit conditions

A—800,000—345	*G*—250,000—138
B—800,000—230	*H*—100,000—230
C—500,000—345	*K*—100,000—138
D—500,000—230	*L*— 50,000—138
E—250,000—345	*M*— 50,000— 69
F—250,000—230	

(First figure represents kilowatts transmitted; the second, kilovolts of sending-end voltage)

The 250-mile lines are equipped with one intermediate synchronous condenser; the 500-mile lines, with two intermediate condensers, in capacity sufficient for 100 per cent load

In Figure 4 the transient-stability limit is represented as a function of the length of the transmission line. In like fashion it may be shown as a function of the sending-end voltage, or of the kilovolt-ampere capacity of installed generators. The representation is similar to that of the steady-state-stability limit (see Figure 2). The curves are also susceptible to interpolation and may be used for similar objectives (see discussion of the steady-state-stability limit).

The relative effect, however, of the system reactance and the transmission voltage is different in the case of the transient-stability limit from that of the steady-state limit. In the transient state the generator reactance is much smaller and the line reactance greater than in the steady state. Therefore, while in the steady state the generator reactance predominates even in the longer lines, in the transient state the reverse is true; here the line reactance is of greater significance, the line length assuming the controlling influence. In the greater majority of cases the total system reactance in the transient state is smaller than in the steady state under normal circuit conditions. As an indirect result, the transmission voltage is a considerably more powerful factor in fixing the magnitude of the transient-stability limit than in fixing that of the steady-state limit.

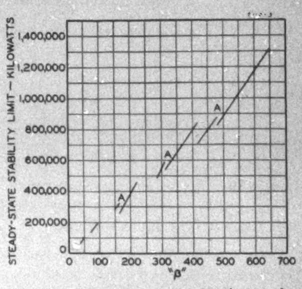


Figure 3. Estimating curves for the steady-state-stability limit

Normal circuit conditions

Curves for systems with installed generator capacity of 55,000 to 950,000 kva, transmitting power to a metropolitan-load center 50 to 500 miles distant, at sending-end voltages of 69, 138, 230, and 345 kv. All systems with receiving-end synchronous condensers in sufficient kilovolt-ampere capacity to maintain a five per cent voltage drop in the line. The 250-mile lines are equipped with one intermediate synchronous condenser; the 500-mile lines, with two intermediate condensers, in capacity sufficient for 100 per cent load. Curve sections denoted *A* are for systems with intermediate condensers. No other means are used for the increase of system stability limits. For explanation of quantity "β," see text of paper

Curves for Estimating the Transient-Stability Limit

As a result of further analysis, the transient-stability limit is represented as a function of the quantity β similar to that used for the representation of the steady-state limit. Thus a curve is obtained for the rapid estimation of the transient-stability limit.

In this case the quantity β has a different meaning from that of the denominator in equation 1, in that X is the total system reactance as taken for the transient-stability calculations, expressed in ohms on the system generator base. The transient reactance of generators and the transmission-line reactance under emergency circuit conditions must now be taken.

The curve thus obtained (Figure 5) permits the estimation, within engineering accuracy, of the transient-stability limit for a system with a double line-to-ground fault cleared in 10.5 cycles. For reasons mentioned above, there is no need for a

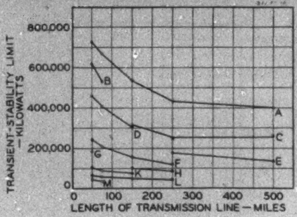


Figure 4. Transient-stability limit

Double line-to-ground fault at the sending end cleared in 10.5 cycles, with loss of circuit or a section of it. For explanation of identification letters, see Figure 2. For circuit condition after fault is cleared, see Figure 1

separate curve for lines equipped with the intermediate synchronous condensers.

The discussion of the β curve and its application in the case of the steady-state-stability limit applies equally well to the transient-stability limit. In addition, it may be said that since the inertia constant of machines in the metropolitan-load center was chosen so as to be representative of actual systems, an additional argument exists for the statement that the curve may be used on systems with loads met in actual practice.

The transient-stability limit given by the curve of Figure 5 may be expressed as follows:

$$\text{Transient-stability limit} = 800 \cdot \beta \text{ kilowatts} \quad (3)$$

with β as defined above. When use is made of the relationship between the severity of various types of faults, such as are given by the curves of Figure 11 of the First Report of Power-System Stability, the following expressions may be obtained for the transient-stability limit at 10.5 cycles' fault-clearing time:

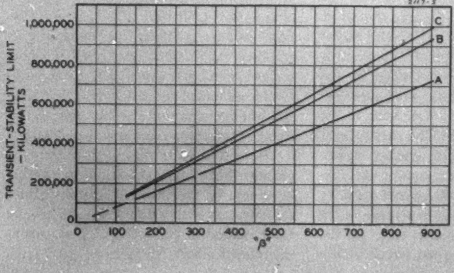


Figure 5. Estimating curves for the transient-stability limit

Sending-end fault cleared in 10.5 cycles

A—Double line-to-ground
B—Line-to-line
C—Line-to-ground

In using the curves for estimating purposes, no distinction is made between systems with and without intermediate synchronous condensers. For general note, see Figure 3

For a line-to-line fault

$$\text{Transient-stability limit} = 1{,}040 \cdot \beta \quad (4)$$

For a line-to-ground fault

$$\text{Transient-stability limit} = 1{,}100 \cdot \beta \quad (5)$$

with β as defined above, and the transient-stability limit expressed in kilowatts.

The above expressions may be easily modified to apply for faults of any duration, by using a curve representing the variation of the transient-stability limit with the clearing time of the particular fault. For this purpose, the curves of the First Report of Power-System Stability may be used for trunk transmission lines.

Criteria of the Margin of the Transient-Stability Limit Over the Test Load

In cases where the transient-stability limit of a system is determined on the a-c network analyzer, the better the guesses as to the probable stability limit of the system, the fewer the total number of swing curves to be taken. The procedure may be shortened considerably if use is made of the relative time when, on the one hand, the maximum angle between the generator and the load machines is reached, and on the other hand, the change in the rotor angle of the generator, $\Delta\delta_n$, becomes negative.

When the angle between the machines reaches its maximum before $\Delta\delta_n$ becomes negative, there is a comparatively large margin between the transient-stability limit of the system and the test load. When the maximum machine angle is reached simultaneously with $\Delta\delta_n$ passing through zero, there still is a margin—though a small one—between the stability limit and the test load. When, however, the angle $\Delta\delta_n$ becomes negative before the maximum value of the angle between the machines is reached, then, practically speaking, there is no margin, and the test load may be taken as equal to the transient-stability limit of the system. Observation of the time relationship given makes it possible to reduce considerably the effort required for the study of the system on the network analyzer.

Part II

From the data on stability limits of the transmission systems investigated on the a-c network analyzer, given in part I of the paper, it may be seen that only a handful of the systems will deliver the 100 per cent block of power to the metropolitan-system load under the fault conditions prescribed, without the use of special design features. Special designs must be applied to the great majority of systems in order that their transient-stability limits may be increased.

It is the purpose of part II of this paper to investigate, on the a-c network analyzer, some of the means for improving the stability limits of transmission systems, and then to apply the various means to the systems whose transient-stability limits had to be raised to the 100 per cent rating.

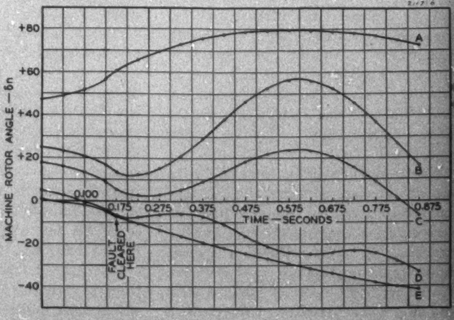


Figure 6. System swing curves, taken on the network analyzer (system 0-25a)

13 per cent resistance in sending-transformer neutral. Double line-to-ground fault at the sending end. System stable

A—Generator
B—Intermediate condenser at point two thirds from receiving end
C—Intermediate condenser at point one third from receiving end
D—Receiving-end condenser
E—Load

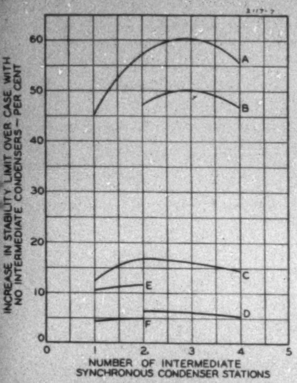


Figure 7. Effect of intermediate-synchronous-condenser stations on system stability limit

Intermediate-condenser stations so located as to divide the line into sections of equal length

A—Steady-state-stability limit—500-mile line
B—Steady-state-stability limit—500-mile line. Reactors substituted for condensers
C—Transient-stability limit—500-mile line
D—Transient-stability limit—500-mile line. Reactors substituted for condensers
E—Steady-state-stability limit—250-mile line
F—Transient-stability limit—250-mile line

Table IV. System Steady-State and Transient-Stability Limits

System Identification Number	Steady-State-Stability Limit				Transient-Stability Limit (Double Line-to-Ground Fault)				Remarks
	Normal Circuit Conditions		Emergency Circuit Conditions		At 10.5 Cycles Fault-Clearing Time		At Zero Fault-Duration Time		
	(Per Cent)	(Kw)	(Per Cent)	(Kw)	(Per Cent)	(Kw)	(Per Cent)	(Kw)	
0-1*a*	193	96,500	162.5	81,250	89.2	44,600	131.5	67,500	
0-2*a*	220	110,000	208	104,000	129	64,500	190	95,000	
0-3*a*	162	81,000	120	65,000	74	37,000	109	54,500	
0-4*a*	200	100,000	190	95,000	115	57,500	169.5	84,750	
0-5*a*	155	77,500	145.5	72,750	99.5	49,750	146.6	73,300	
0-6*a*	147	73,500	132.5	66,250	84.5	42,250	124.5	62,200	One *IC* at midpoint
0-7*a*	196	196,000	177	177,000	105	105,000	154.5	154,500	
0-8*a*	183	183,000	159.5	159,500	91	91,000	134	134,000	
0-9*a*	134	134,000	120	120,000	76	76,000	112	112,000	
0-10*a*	160	160,000	152	152,000	99.7	99,700	147	147,000	
0-11*a*	164	164,000	152	152,000	88.7	88,700	130.8	130,800	One *IC* at midpoint
0-12*a*	167	417,000	138.5	346,000	74	185,000	109	272,500	
0-13*a*	184	460,000	168.5	421,000	97	242,500	143	357,000	
0-14*a*	167	417,000	148.5	371,000	84.2	210,500	124.2	315,000	
0-15*a*	129	322,500	106	265,000	63	157,500	92.8	232,000	
0-16*a*	118	295,000	89	222,500	47.9	119,900	70.5	176,500	One *IC* at midpoint
0-17*a*	135	337,500	116	290,000	71	177,500	104.6	261,500	One *IC* at midpoint
0-18*a*	122.5	306,000	95.5	239,000	54.5	136,300	80.2	200,500	Two *IC* equally spaced
0-19*a*	167	835,000	158	790,000	91.7	459,000	135.2	675,000	
0-20*a*	151	755,000	140	700,000	81.6	408,000	120.3	601,500	
0-22*a*	118	590,000	104	520,000	61	305,000	89.7	444,000	
0-23*a*	128	640,000	108	540,000	62.7	314,000	92.4	462,000	
0-24*a*	118	590,000	92	460,000	49.5	247,600	72.9	364,500	One *IC* at midpoint
0-25*a*	97	485,000	83	415,000	52.5	262,500	77.3	386,500	Two *IC* equally spaced
0-26*a*	149	1,194,000	138	1,105,000	77.4	619,500	114	912,000	
0-27*a*	165	1,321,000	159	1,272,000	90.8	727,000	134	1,074,000	
0-28*a*	129	1,033,000	116.5	932,000	66.4	531,000	97.8	782,000	
0-29*a*	146	1,170,000	138.5	1,110,000	83.6	669,500	123.4	988,000	
0-30*a*	120	960,000	109.5	876,000	67.1	537,000	98.9	792,000	
0-31*a*	109.5	876,000	96	768,000	53.9	431,200	79.3	634,000	One *IC* at midpoint
0-32*a*	90	720,000	80	640,000	49.8	398,200	73.4	587,000	Two *IC* equally spaced

Systems have generators of normal design and are not equipped with devices for stability improvement, except with intermediate synchronous condensers, as indicated under "Remarks."

IC indicates an intermediate synchronous condenser, placed for 100 per cent load.

For list of systems, see Table I.

The intermediate synchronous condenser, the resistance in the neutral of the sending transformer, and the reactor on the generator bus are the three devices for increasing the stability limits which were investigated. They were chosen, because the available data in the literature on their application are either incomplete or nonexistent, and also because they were within the means available for the type of academic study made by the writer.

The Intermediate Synchronous Condenser

There are relatively few power-transmission systems on which intermediate synchronous condensers are installed. Wherever this is the case, however, satisfaction is generally expressed with the part the intermediate condenser plays in the system operation. Since in most systems the installation of the intermediate condenser was made previous to the time when the full significance of the problem of transient stability was realized it may be presumed that the primary reason for installing the condenser was its contribution to the steady-state-stability limit and to the maintenance of voltages at certain points of the transmission line. Indeed, the studies published by Baum,[10,11] Fortescue,[12,13] Fortescue and Wagner,[14] Evans and Bergvall,[15] Petersen,[16] Piloty,[17–19] and Rüdenberg[20,21] deal exclusively with the effect of the intermediate condenser on the steady-state-stability limit of the system. None of these studies were made on the network analyzer, and consequently the method of approach necessitated simplifying assumptions; for instance, in their study of the intermediate condenser, Wagner and Evans[22] assumed an infinite bus (that is, a bus to which so large a synchronous capacity is connected that its frequency and voltage are absolutely unaffected by any conditions external to that bus). There are practically no published data on the effect of the intermediate condenser on the transient-stability limit. Summers and McClure[23] mention the subject briefly, and Tchernycheva and Lavrov[24] discuss the effect of the intermediate condenser on the system stability limit for the case of a three-phase fault.

From the study of system operation it appears that all intermediate condensers on a line may operate either primarily leading or primarily lagging; or that some will operate mostly leading, requiring a 1.0 to 0.5 lead-to-lag ratio, while others will operate only lagging, requiring a 1.0 to 1.0 lead-to-lag ratio. This depends on the length of the line, on the number of condenser stations, and on the ease with which the line carries its 100 per cent load. For instance, on the 500-mile lines with two intermediate condensers, both condensers have a 1.0 to 1.0 lead-to-lag ratio; while with three condenser stations, two condensers have a 1.0 to 1.0 lead-to-lag ratio and the third has a ratio of 1.0 to 0.5. In every case the larger condenser kilovolt-amperes are required under emergency circuit conditions. The kilovolt-ampere capacity of the intermediate condensers and the total synchronous-condenser capacity for the systems under design, for the number of intermediate-condenser stations as finally adopted, are given in Table V.

Table V. Kva of Synchronous Condensers in the 250- and 500-Mile Systems (Receiving-End and Intermediate Condensers) as Determined on the A-C Network Analyzer

		Intermediate Synchronous Condensers			Total Synchronous Condensers in System		
System Identification Number	Receiving-End Synchronous Condensers Total Kva	Total Kva	No. of Intermediate Condenser Stations	Condenser Lead-to-Lag Ratio	Total Kva	Condenser Kva Per Kw of Line Rating Kva/Kw_r	Condenser Kva Per Kw of Steady-State Limit Under Normal Circuit Conditions Kva/Kw of SSSL–N
0-6*a*	32,100	20,500	One	1.0:1.0	52,600	1.05	0.72
0-11*a*	61,100	69,750	One	1.0:1.0	130,850	1.31	0.80
0-16*a*	285,000	349,000	One	1.0:0.5	634,000	2.54	2.15
0-17*a*	203,000	132,500	One	1.0:1.0	335,500	1.34	0.99
0-18*a*	214,000	313,400	Two	1.0:1.0	527,400	2.11	1.72
0-24*a*	537,000	608,000	One	1.0:0.5	1,145,000	2.29	1.94
0-25*a*	410,000	369,000	Two	1.0:1.0	779,000	1.56	1.61
0-31*a*	772,000	386,700	One	1.0:0.5	1,158,700	1.44	1.32
0-32*a*	602,500	325,500	Two	1.0:1.0	928,000	1.16	1.29

For commentary on system identification numbers, see part I, Table I.

Condenser kilovolt-amperes given leading.

Intermediate synchronous condensers located so as to divide line into sections of equal length, and placed for 100 per cent load.

The swing curves of two intermediate condensers on a 500-mile line loaded almost up to its transient-stability limit are shown in Figure 6.

The results of a study of the effect of the intermediate synchronous condensers on the stability limits of the 250-mile and 500-mile systems may be summarized as follows: On an average 250-mile line, with one intermediate-condenser station in the system, the steady-state-stability limit is about ten per cent greater than in the system with no intermediate condensers. The stability limit of a 500-mile line, under the same conditions, is 45 per cent greater. On a 250-mile line carrying its 100 per cent load very easily, the beneficial effect of the intermediate condenser may be considerably greater; for instance, in the system 0-11*a*, which typifies a light line, the steady-state-stability limit is increased by about 20 per cent.

The increase in the transient-stability limit caused by one intermediate-condenser station is far smaller than the increase in the steady-state limit. On an average 250-mile line the increase is less than five per cent; on a 500-mile line it is about 12 per cent. Thus it is seen that the intermediate condenser is more effective on the longer lines than it is on the shorter ones.

As the number of intermediate-condenser stations on the line is increased, the stability limits increase until a certain theoretical optimum is reached. On a 250-mile line the difference in stability limits when one or two intermediate stations are installed is so small that from the economic viewpoint one station will give the best solution. On a 500-mile line, however, the largest increases in the steady-state-stability limit seem to take place with a different number of condenser stations than is best for the transient-stability limit. Economic computations, however, will show that two stations are the optimum. Two stations will increase the steady-state-stability limit by about 57 per cent and the transient limit by about 16 per cent.

Irrespective of the number of intermediate-condenser stations in the system, their location is most advantageous when they divide the entire line into sections of equal length; thus, for a 250-mile line, a mid-point condenser gives the best result.

Doubling the size of the intermediate condenser required to maintain specified voltages increases both the steady-state- and the transient-stability limits by slightly less than ten per cent over the case of a condenser of normal size. The same general result is obtained when intermediate condensers of lower reactance are used.

Study of the substitution of reactors for lagging intermediate condensers was made on systems where all condensers operate fully lagging, as well as on those where some condensers operate lagging while others are leading. The results show that when lagging condensers are replaced by reactors, the stability limits are considerably lowered, especially the transient-stability limit. While in a certain 500-mile system the increase in the steady-state limit obtained from reactors was only ten per cent less than that obtained from synchronous condensers, the increase in the transient-stability limit was less than half that obtained with condensers.

Reactors therefore can be substituted for lagging intermediate condensers only when the steady-state stability is considered of greater importance than the transient stability, and provided the lower initial cost and lower losses make the over-all economic balance in favor of the reactors. The effect of intermediate condensers on system stability limits is represented in Figure 7.

When the load in a 500-mile system drops below the 100 per cent level for which the intermediate condensers are provided, then the voltage at the intermediate points may rise considerably above the desired level and thus endanger the insulation of the system and the continuity of operation. This may also take place in some, though not all, 250-mile lines. Intermediate condensers of sufficient capacity for the 100 per cent load are insufficient to prevent the rise in voltage in these lines with smaller loads. On a 500-mile line, with a 50 per cent load, the intermediate-condenser capacity needed is about double that required for the 100 per cent load; on the 250-mile line it is approximately 25 per cent greater, when equal voltages are to be maintained with both the 50 and 100 per cent loads. It is not economical to provide intermediate condensers large enough to keep normal voltages at very low loads; yet they should be placed for a load smaller than 100 per cent. This will put the system in a more favorable operating condition and will facilitate the charging problem of the line. A corresponding increase in the stability limits of the system will result; this will compensate for the additional cost of the condenser.

Resistance in the Neutral of the Sending Transformer

It has been recognized for some time that the grounding of the neutral of the sending transformer through a resistance has a distinctly beneficial effect on the transient-stability limit of the system. Although this method of grounding has been applied to existing systems, the published data on its effect on the transient-stability limit are very meager. It has been reported,[8] however, that with the sending-end double line-to-ground faults, which are also assumed for the present study, the same percentage gain in the transient-stability limit is obtained when either the neutral of the sending-end

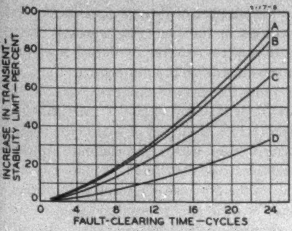


Figure 8. Effect on transient-stability limit of a 13 per cent resistance in the sending-transformer neutral

Double line-to-ground fault at the sending end

A—50-mile line
B—75-mile line
C—150-mile and 500-mile lines
D—250-mile line

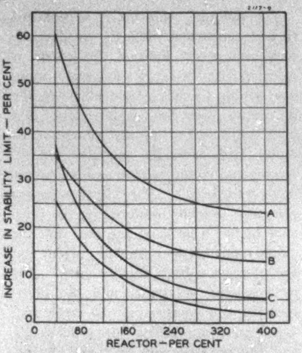


Figure 9. Effect of shunt reactors on generator bus on stability limits of 250-mile to 500-mile lines

Lines equipped with intermediate synchronous condensers

A, B—Steady-state-stability limit
C, D—Transient-stability limit

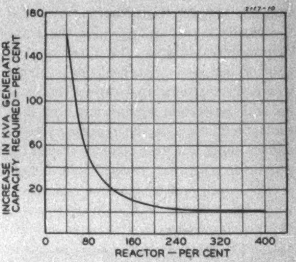


Figure 10. Effect of shunt reactors on generator bus on required kilovolt-ampere generator capacity on 250-mile to 500-mile lines

Lines equipped with intermediate synchronous condensers

transformer alone is grounded through a resistance, or when, in addition to it, the neutral of the receiving-end transformer is grounded through a reactance. Hence, so far as the transient stability is concerned, the grounding of the receiving-transformer neutral is not necessary with the type of fault being considered.

In grounding the sending-transformer neutral of a 50-mile line with various resistors ranging from 5.8 to 17.5 per cent, it was found that the smallest angle between the machines at the ends of the system at the transient-stability limit occurs with a 13 per cent resistance. This optimum resistance, however, is not particularly critical, since the observed variations in the angles between the machines with the various resistor sizes are accompanied by very small differences in corresponding transient-stability limits. This leads to the conclusion that the ultimate selection of a resistance—whether it should be 13 per cent or should differ somewhat from it—must be based on considerations other than stability; namely, on the effect of the size of the resistance on the insulation of the transformer. On this basis, in all systems designed, the study of the effect on the transient-stability limit of grounding the sending-transformer neutral was made with grounding through a 13 per cent resistance.

The increase in the transient-stability limit obtained at various fault-clearing times on lines from 50 to 500 miles along is given in Figure 8. The curves may be judiciously interpolated for any length of line up to and somewhat exceeding 500 miles.

It is seen from the data obtained that the resistance in the sending-transformer neutral makes a very substantial contribution to the transient-stability limit. The percentage increase secured on a 50-mile line is the largest; it then decreases as the line length increases, reaching a minimum on lines somewhat over 250 miles in length. This is due to the fact that as the line gets longer, its zero-sequence impedance increases. A decrease in the zero-sequence current follows and causes in turn a smaller I^2R loss in the grounding resistor. As a consequence, the power output of the generator under fault conditions is not maintained so well on the longer lines as on the shorter. The resultant increase in the transient-stability limit in the 250-mile lines is smaller than in the 50-mile lines.

Shunt Reactors on the Generator Bus

It was suggested some years ago that shunt reactors be used on the generator bus[25] in order to increase the stability limits of the system, and views were expressed to the effect that this method has some real possibilities. There are, however, no published data on the subject.

The writer does not know of any case where such reactors are installed on systems in this country. From a sketchy description[26,27] of the Kembs–Creney 220-kv, 173-mile line in France, it appears that there is a 15,000-kva shunt reactor on the generating bus of the station at Kembs. A statement is made that the reactor renders a very satisfactory service in helping to maintain stability of the line.

The shunt reactor on the generator bus is connected directly from the low-voltage sending-end bus to ground. The reactor increases the total system transfer reactance, it lowers the power factor on the generator, and increases its excitation voltage. The increase in the total system transfer reactance lowers the system stability limit, while the increase in the excitation voltage raises it. As a result, an important increase in the system stability limit is obtained. Since the generators must supply the transmission line with the same amount of active power when the generator bus is loaded with reactors as when they are not used—and this without overheating during the operation with full-load normal field currents—the installed kilovolt-ampere capacity of generators, in the case when reactors are used, must be greater than when there are none.

The study of the shunt reactor on the generator bus was made on the 250-mile and 500-mile systems on the network analyzer and was checked by analytical calculations. Reactors of from 40 to 400 per cent were placed on the 250-mile systems, equipped with one intermediate condenser, and on the 500-mile systems with two condensers. When the reactor was connected to the generator bus, the kilovolt-ampere generator capacity was increased to satisfy the new conditions. The increases in the steady-state- and transient-stability limits obtained on these systems are represented in Figure 9.

It is seen that the shunt reactor on the generator bus greatly increases the system stability limits. In the systems investigated, a 40 per cent reactor increased the steady-state limit as much as 60 per cent, and the transient limit as much as 37 per cent. The increase in the transient limit is considerably smaller than that in the steady-state limit; still it is very substantial. Figure 9 gives two curves for the steady-state- and two for the transient-stability limits. The upper of each pair of curves is obtained for systems where the relationship between the reactor and the system reactance is such that the introduction of the reactor, accompanied with a corresponding change in generator reactance, causes little change in the total system transfer reactance, while the lower of each pair of curves is for systems where this change is appreciable. Thus the percentage increase in the steady-state- or transient-stability limits obtained from a reactor of a certain size lies between the upper and lower curves of a pair, depending on the system. As the size of the reactor increases, its effectiveness declines. The curves of Figure 9 may be used for estimating the range of the increase in stability limits caused by the presence of a reactor on the bus.

A part of the increase in the stability limits of a system with a shunt reactor on the bus is due to the larger kilovolt-ampere capacity of generators required by the reactor. A study of the two factors, made separately on a system having a steady-state-stability limit of 118 per cent, with a normal kilovolt-ampere generator capacity, gave the following results: With a reactor on the bus and the generator capacity correspondingly increased, the steady-state limit rose to 157 per cent; when the reactor was removed, with the increased generator capacity still connected to the system, the stability limit dropped to 132 per cent. The contribution of the reactor itself to the stability limit is plainly evident.

The increase in the steady-state-stability limit caused by the shunt reactor on the bus, as measured on the network analyzer, may be checked in the following fashion: The ratio of the generator excitation voltages for the system with and without a reactor on the bus is first determined. This may be calculated from the voltage vector diagram. The increased kilovolt-ampere generator capacity and the actual angle between the line current and the reference voltage (line sending-end voltage) must be properly represented. The ratio of the total system transfer reactance, without a reactor on the bus, to that with it is then calculated. The product of the two ratios will indicate the increase in the steady-state-stability limit. The result differs from the measured figures by 15 to 20 per cent. This is due to the fact that the excitation voltage is determined for the condition of 100 per cent load, and not at the pull-out for which the stability limit is determined on the network analyzer.

The pull-out condition is approached to a greater extent when the steady-state maximum power limit of the system is calculated at the rated voltage in a manner similar to that employed when the maximum power limit is determined of a long transmission line and synchronous-motor load, with the sending- and receiving-end voltages given and reactances only considered.[9] The kilovolt-ampere generator capacity required by the presence of the reactor on the bus, and the actual angle between the line current and the reference voltage must be taken. The receiving-end condenser and the intermediate condensers may be neglected. The line sending-end leak also is neglected, and the receiving-end leak may be assumed to equal infinity. This method of calculation does not represent a rigorous solution; still it gives a satisfactory check with the data obtained on the network analyzer.

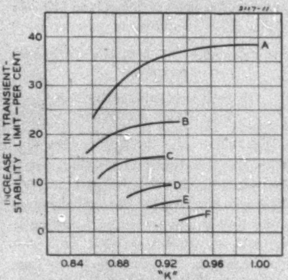


Figure 11. Curves for estimating the increase in transient-stability limit due to shunt reactors on generator bus

Based on study of 250-mile to 500-mile systems with intermediate synchronous condensers. For explanation of quantity *k* see text of paper

Reactor size
A—40 per cent
B—90 per cent
C—130 per cent
D—200 per cent
E—260 per cent
F—400 per cent

The kilovolt-ampere generator capacity required by shunt reactors on the bus is easily calculated when the angle by which the line current leads the sending-end voltage is known. On the 250- to 500-mile lines studied, with one or two intermediate condensers, this angle varies within narrow limits. The increase in generator kilovolt-amperes required by reactors of various sizes in the systems under design is given in Figure 10. With a 40 per cent reactor the increase is over 150 per cent, dropping rapidly as the size of the reactor increases. With a change in the number of intermediate-condenser stations in the line, the generator kilovolt-amperes required by a reactor on the bus will undergo a slight change.

From the effect of shunt reactors on the transient-stability limit of various systems, a set of curves of Figure 11 is obtained from which the increase in the transient-stability limit of the system due to the presence of a reactor on the bus may be estimated when the ratio *k* of the total system transfer reactance without the reactor to that with a reactor is calculated. In determining the ratio *k*, the transient reactances of the generators and the reactance of the line under emergency circuit conditions must be taken. For the system transfer reactance, with the reactor on the bus, the increased generator kilovolt-amperes should be taken as required by the reactor, and the architrave of the equivalent π circuit of the entire circuit must be known. It is also possible to determine from the curves the proper size of reactor to be placed on the generator bus when it is desired to increase the transient-stability limit of the system by a certain percentage, when the system reactances are known. The curves may be applied to double line-to-ground, line-to-line, and line-to-ground faults, and are subject to interpolation.

The increase in the steady-state- and transient-stability limits caused by the presence of the shunt reactor on the generator bus is substantially independent of the length of the line. The increase in the transient-stability limit caused by the reactor is essentially independent of the type of fault and of the fault-clearing time.

In view of the large increase in the kilovolt-ampere generator capacity required by some shunt reactors on the generator bus, the economic benefits from the increase in stability limits due to their presence must be balanced against the cost of the reactors, as well as the expense of the incremental generating capacity which must be installed, including associated housing and other equipment. It should be borne in mind, however, that it is necessary to increase the capacity of the prime-mover equipment only to the extent of supplying the losses in the incremental generating capacity and in all reactors. In so far as reactors are concerned, air-core units may be used. In general, the installation of shunt reactors on the generator bus would be economical in cases of which the following may be representative:

1. When the reactors require a comparatively small increase in generator capacity.
2. When they are to be installed in a system where the total installed generating

capacity is small, so that the addition to it of the high percentage required by the reactors is not excessive.

3. When a transmission line is fed from a bus of a large generating system supplying a large local load at a small annual load factor, so that reactors may be installed without adding to the existing generating capacity.

Other Methods for the Increase of Stability Limits

Various means have been proposed to increase the stability limits of transmission systems. Of these, such methods as shunt static condensers, series synchronous condensers, inductive compensating generator, series loading resistors, quadrature booster transformers, tuned line, neutralizing networks, frequency changers, and other conversion equipment have not been used in the present study. High-speed exciters are not used, because they bring about a very small gain in the stability limit at fast fault-clearing times. The construction of additional circuits or the shortening of the line sections provides only a slight increase in the stability limit, and hence both are hardly economical.

Series capacitors are another major device proposed to overcome stability limitations of long lines. It has recently been shown[23] that series capacitors offer great promise so far as the steady-state-stability limit is concerned. They should, however, be studied under fault conditions in order not only to ascertain to what degree they are effective in overcoming the transient-stability limitations, but also to make sure that they operate properly at the high voltages which under fault conditions may take place in the 230- and 345-kv lines. The means for such an investigation were not at the disposal of the writer, and so this device was not applied to the systems reported in this study.

Four additional methods which are used for the increase of stability limits of the systems designed are:

1. The decrease of the transient reactance of the generators.
2. The increase of their inertia constant.
3. Generator damper windings.
4. High-speed switching.

All these methods are well known; nevertheless the following will be pointed out:

The increase in the transient-stability limit, because of the decrease of the generator transient reactance, is essentially independent of the fault-clearing time, while that obtained from the increase of the inertia constant does depend upon it. On the other hand, the beneficial effect obtained from lowered transient reactance is usually secured at a slightly higher cost. In some systems the inertia of the receiving-end machines may be controlled by the system designer; in the systems of this study the constants of the receiving-end rotating equipment are assumed to be fixed. Damper windings of both high and low resistance serve to improve the transient-stability limit; the former provide negative-sequence damping while a dissymmetrical fault lasts, while the latter have an appreciable positive-sequence damping action after the fault is cleared. Whenever, in the systems reported, dampers are said to be required, they are assumed to be of high resistance. It is felt, however, that the type of dampers to be used must be decided on the merits of the individual case, and that this has little effect on the magnitude of the stability limit. It should be borne in mind that the increase in the transient-stability limit due to the dampers depends on the switching time as well as on the type of grounding of the sending-transformer neutral. By far the most effective factor affecting the transient-stability limit of a system is the fault-clearing time; the high-speed circuit breaker may hence be considered the major device for this purpose. The effect of the fault-clearing time on the increase of the stability limit, with various types of faults, is well known and has already been reported.

In every long transmission system the tower-footing and fault resistance makes a small contribution to the increase of its transient-stability limit. In the zero-sequence network represented on the network analyzer, the tower-footing resistance was omitted. Still, in each system a small fraction of the ultimate limit of its transient stability is credited to the improvement caused by the tower-footing and fault resistance.

Design Features Required by the Transient-Stability Conditions Imposed

From the transient-stability limit of systems at 10.5 cycles fault-clearing time, as given in Table IV (part I of paper), it will be seen that there is a group of six systems, namely, systems 0-2*a*, 0-4*a*, 0-5*a*, 0-7*a*, 0-10*a*, and 0-13*a*, whose transient-stability limit is either very close to 100 per cent or exceeds it. These systems therefore require no special devices for the increase of stability limits. In fact, in some cases the fault-clearing time could exceed 10.5 cycles and still the condition of 100 per cent transient stability would be met. In spite of this, standard eight-cycle circuit breakers are provided for these systems, thus securing in some of them a margin between the rating assigned and the transient-stability limit. The additional generators and transformers which this would permit are not installed at this time; however, these systems will be somewhat more economical when full advantage of transmission capacity is taken. The problem of system expansion is not considered in this study.

The remaining systems are divided into two groups. To the first group belong systems whose transient-stability limit is below 100 per cent but which have a reasonable margin—from 16.0 to 62.5 per cent—between the line rating and its steady-state-stability limit under emergency circuit conditions. These are systems 0-1*a*, 0-3*a*, 0-6*a*, 0-8*a*, 0-9*a*, 0-11*a*, 0-12*a*, 0-14*a*, 0-17*a*, 0-19*a*, 0-20*a*, 0-26*a*, 0-27*a*, and 0-29*a*. Here, as a rule, the high-speed circuit breaker is used to increase the transient-stability limit; and in addition to this, where it is found necessary, either the generator transient reactance is reduced from 35 per cent standard value to 30 per cent, or a damper winding is added. In some systems of this group it sufficed to lower the generator transient reactance to 30 per cent, without having to apply high-speed circuit breakers. On the three 250-mile lines the mid-point intermediate condenser of kilovolt-ampere capacity necessary for a 50 per cent system load has been installed. This is resorted to primarily for the purpose of increasing the transient-stability limit rather than for any other reason.

To the second group belong systems with a small margin between the line rating and its steady-state-stability limit under emergency circuit conditions. These are systems 0-15*a*, 0-16*a*, 0-18*a*, 0-22*a*, 0-23*a*, 0-24*a*, 0-25*a*, 0-30*a*, 0-31*a*, and 0-32*a*. Here the steady-state-stability limit under emergency circuit conditions is either 4.0 to 8.5 per cent above the line rating or 4.5 to 20.0 per cent below it. In these systems the steady-state-stability limit is raised first by lowering considerably the generator transient reactance. This affects the short-circuit ratio of the generator, and through it the steady-state-stability limit of the system. For the same purpose, the generator bus is loaded with shunt reactors, where necessary, with a corresponding increase of the kilovolt-ampere generator capacity. On all the 250-mile and 500-mile lines of this group, intermediate condensers are installed for a 50 per cent load on the system.

Table VI. Special Design Features Necessary for the Transmission of 100 Per Cent Rated Power at a Double Line-to-Ground Fault

System Identification Number	Fault-Clearing Time Required (Cycles)	Circuit-Breaker Opening Time (Cycles)	Generator Transient Reactance (Per Cent)	Generator Inertia, Per Cent of Standard	Generator Damper Winding	Grounding Sending-Transformer Neutral (Per Cent Resistance)	Shunt-Reactor on Generator Bus (Per Cent Reactance)	Increase in Generator Kva Required by Reactor on Generator Bus (Per Cent)	Note on Intermediate Synchronous Condensers (IC)
0-1*a*	9.5	8.0	30.0						
0-2*a*	9.5	8.0							
0-3*a*	4.5	3.0							
0-4*a*	9.5	8.0							
0-5*a*	9.5	8.0							
0-6*a*	9.5	8.0	30.0						One *IC*
0-7*a*	9.5	8.0							
0-8*a*	9.5	8.0	30.0						
0-9*a*	5.5	4.0							
0-10*a*	9.5	8.0							
0-11*a*	9.5	8.0							One *IC*
0-12*a*	4.5	3.0			Dampers				
0-13*a*	9.5	8.0							
0-14*a*	7.5	6.0							
0-15*a*	9.5	8.0	17.5	200		13			
0-16*a*	4.5	3.0	17.5	200	Dampers	13	52	100	One *IC*
0-17*a*	4.5	3.0							One *IC*
0-18*a*	9.5	8.0	17.5	200		13	125	20	Two *IC*
0-19*a*	9.5	8.0	30.0						
0-20*a*	7.5	6.0	30.0						
0-22*a*	7.5	6.0	20.0	200		13			
0-23*a*	9.5	8.0	17.5	200	Dampers	13			
0-24*a*	5.5	4.0	17.5	200		13	70	62	One *IC*
0-25*a*	9.5	8.0	17.5	200		13	90	40	Two *IC*
0-26*a*	5.5	4.0	30.0						
0-27*a*	9.5	8.0	30.0						
0-28*a*	9.5	8.0	22.5	200		13			
0-29*a*	7.5	6.0	30.0						
0-30*a*	9.5	8.0	25.0	200	Dampers	13			
0-31*a*	7.5	6.0	17.5	200	Dampers	13	110	27	One *IC*
0-32*a*	7.5	6.0	17.5	200		13	90	40	Two *IC*

Where not specifically indicated, generator transient reactance and generator inertia are standard (as taken in the study).

Per cent reactor on generator bus is on the generator base prior to the increase in generator kilovolt-amperes required by the reactor.

Intermediate synchronous condensers placed in kilovolt-ampere capacity for 50 per cent load.

Although here the primary purpose is to prevent the voltage from rising with a smaller than 100 per cent load, the installed intermediate condensers serve well the purpose of increasing the system steady-state-stability limit. The margin between the system rating and its steady-state-stability limit under emergency conditions having been improved, means are applied which increase primarily the transient-stability limit, such as grounding of the sending-transformer neutral, increased generator inertia, damper windings, and high-speed circuit breakers. It will be noted that the transient-stability limit of these systems as originally obtained is well below 100 per cent; hence the application of several devices is required. System 0-28*a*, although by definition not in the second group of systems, is treated substantially in a similar manner.

Since the opening time of the high-speed circuit breakers is not standardized, their speeds have been specified as required by the system analysis. In every system, from one to three per cent of the stability limit attained is credited to the tower-footing and fault resistance.

Table VI gives the list of the various methods for overcoming stability limitations which were applied to the systems studied. With these design features the transient-stability limits are brought to the 100 per cent system rating with a double line-to-ground fault. Additional calculations will show the changes necessary when other types of faults are considered. The methods applied in particular cases may be somewhat modified, according to the preference of the designer.

The table shows very interesting examples of the various methods required for the improvement of stability when either the magnitude of the block of power transmitted, the transmission voltage, or the length of the line is modified. The results of changing the transmission voltage alone may be observed in comparing system 0-3*a* with 0-4*a*, 0-12*a* with 0-13*a*, or 0-16*a* with 0-17*a*. In case 0-16*a*, for example, it was necessary to apply practically all methods of improvement in order to transmit 250,000 kw at 230 kv over a distance of 250 miles; in case 0-17*a*, however, when 345 kv was used as the sending-end voltage, the same result was accomplished with generators of standard design using three-cycle circuit breakers.

Summary and Conclusions

Results of the system study may be summarized as follows:

1. The steady-state-stability limit is given for a number of systems transmitting 50,000 to 800,000 kw (at the receiving end) over distances ranging from 50 to 500 miles at sending-end voltages of 69 to 345 kv. When represented in the form of curves, the data permit the determination by interpolation of the stability limit of various systems whose transmission distance, voltage, and power are within the range investigated.

2. For the same systems, the transient-stability limit is obtained for the condition of a double line-to-ground fault near the sending-end bus, cleared in 10.5 cycles, with the loss of a circuit or a section of it. The data may be used to determine by interpolation the transient-stability limit of various systems in a manner similar to that used in determining the steady-state limit.

3. The transient-stability limits of two systems of similar configuration are related by a simple expression (equation 2) permitting the calculation of the transient-stability limit of one system from the known transient-stability limit of another system when basic data on both systems are available.

4. A set of curves is suggested from which it is possible to estimate rapidly the steady-state-stability limit of transmission systems of a certain type.

5. Curves and expressions are given from which the transient-stability limit of a system may be estimated for double line-to-ground, line-to-line, and line-to-ground faults (equations 3, 4, and 5) cleared in 10.5 cycles. The possibility of modifying the expressions to be used with faults of any duration is indicated.

6. It is suggested that possibly the time required for determining on the network analyzer the system transient-stability limit may be shortened, by making observations on the angles of the system machines.

7. The intermediate synchronous condenser is far more effective in increasing the steady-state-stability limit of the line than in increasing its transient-stability limit. While on a 500-mile line the optimum number of intermediate-condenser stations increases the steady-state-stability limit by 45 per cent, it increases the transient-stability limit by only about 12 per cent.

8. The intermediate synchronous condenser is more than twice as effective on the 500-mile line as it is on the 250-mile line.

9. On a 250-mile line the optimum number of intermediate-condenser stations is one; on a 500-mile line, two. The location of

intermediate-condenser stations is best where they divide the line into sections of equal length. Doubling the size of the intermediate condenser raises the system stability limits by about ten per cent.

10. Substitution of reactors for lagging intermediate condensers lowers considerably the system stability limits.

11. The primary purpose in placing the intermediate synchronous condensers on long lines should be to raise the steady-state-stability limit of the line, and to prevent the voltage at the intermediate points from rising with smaller than 100 per cent loads. Since the kilovolt-ampere capacity of the intermediate condenser required by the 100 per cent load is smaller than that required by lighter loads, the intermediate condensers should be installed as required by the latter condition.

12. The resistance in the neutral of the sending transformer increases substantially the transient-stability limit. Within the range of the systems investigated, the increase is the largest on a 50-mile line, and reaches a minimum on lines about 250 miles long. As the line length approaches 500 miles, the effectiveness of the resistance increases.

13. Within the investigated range of resistances in the sending-transformer neutral—from 5.8 to 17.5 per cent—no particularly critical value affecting the stability limit has been found, thus leading to the conclusion that selection of the resistance size should be based on considerations other than stability.

14. The shunt reactor on the generator bus increases the stability limits very appreciably. The increase obtained in the steady-state-stability limit is considerably greater than that secured in the transient-stability limit. With a 40 per cent reactor, the steady-state limit increases about 60 per cent, while the transient-stability limit increases more than 35 per cent. The larger the reactor, the smaller its effectiveness. Curves are given showing the increases in stability limits obtained from reactors of various magnitudes.

15. The increase in the stability limits caused by the shunt reactor on the generator bus is essentially independent of the length of the line, the type of fault, and the fault-clearing time.

16. The shunt reactor on the generator bus requires that the installed kilovolt-ampere capacity of generators be correspondingly increased. The smaller the reactor, the larger the required additional generating capacity. Curves are given showing the size of the shunt reactor to be placed when a certain increase in the transient-stability limit is desired; a curve is also given for the required additional kilovolt-ampere generator capacity.

17. The transient-stability limit of each system studied has been raised, where required, to the desired 100 per cent value by applying the various methods serving this purpose. The methods depend on the margin between the desired rating of the system and its steady-state-stability limit under the emergency circuit condition. The results illustrate (Table VI) the effect of the line length and its transmission voltage on the means to be applied.

References

1. Empirical Method of Calculating Corona Loss From High-Voltage Transmission Lines, **J. S. Carroll, Mabel Macferran Rockwell.** AIEE Transactions, volume 56, 1937, May section, pages 558–65; discussion, volume 57, 1938, March section, pages 168–9.

2. Corona Losses From Conductors of 1.4-Inch Diameter, **J. S. Carroll, B. Cozzens, T. M. Blakeslee.** AIEE Transactions, volume 53, 1934, pages 1727–33.

3. Symposium on Operation of the Boulder Dam Transmission Line—Corona Experience on Transmission Line, **B. Cozzens, W. S. Peterson.** AIEE Transactions, volume 58, 1939, April section, pages 137–40; discussion, pages 156–60.

4. Design of Transmission Lines, **J. J. Torok, W. R. Ellis.** *Electric Journal*, volume 30, 1933, pages 467–71, 515–17.

5. Problems of 220-Kv Power Transmission, **A. E. Silver.** AIEE Transactions, volume 38, 1919, pages 1036–1135.

6. Power Transmission and Distribution in the U.S.A., **P. H. Chase, W. W. Lewis, D. M. Simmons, H. R. Woodrow.** *General Electric Review*, volume 35, 1932, pages 431–8, 472–7.

7. Equivalent Circuits in Stability Studies, **G. C. Dahl, A. E. Fitzgerald.** AIEE Transactions, volume 53, 1934, September section, pages 1272–82. (See Figure 13.)

8. First Report of Power-System Stability, AIEE subcommittee on interconnection and stability factors. AIEE Transactions, volume 56, 1937, February section, pages 261–82.

9. Electric Power Circuits, volume II, **G. C. Dahl.** McGraw-Hill Book Company, New York, N. Y., 1938.

10. Voltage Regulation and Insulation for Large-Power Long-Distance Transmission Systems, **F. G. Baum.** AIEE Transactions, volume 40, 1921, pages 1017–77.

11. The Technical Problems of High-Tension Electric Power Transmission, **F. G. Baum.** First World Power Conference, *Proceedings*, volume III, 1924, pages 1110–28.

12. The Baum Principle of Transmission, **C. L. Fortescue.** *Electrical World*, volume 89, 1927, pages 954–60.

13. The Economics of Power Transmission as Influenced by Recent Stability Studies and Increase in Speed of Circuit Interruption, **C. L. Fortescue.** Second World Power Conference, *Proceedings*, 1930, Paper 265.

14. Some Theoretical Considerations of Power Transmission, **C. L. Fortescue, C. F. Wagner.** AIEE Transactions, volume 43, 1924, pages 16–23.

15. Experimental Analysis of Stability and Power Limitations, **R. D. Evans, R. C. Bergvall.** AIEE Transactions, volume 43, 1924, pages 39–58.

16. Energie-Übertragung mit Sehr Hohen Spannungen, **W. Petersen.** *Elektrotechnik und Maschinenbau*, volume 48, 1930, pages 600–02.

17. Leistungsgrenzen und Stabilitaet von Grosskraftübertragungen, **H. Piloty.** *Forschung und Technik*, pages 200–14.

18. Überwachung des Kompensationszustandes in Netzen mit Kompensiertem Erdschlussstrom, **H. Piloty.** *Forschung und Technik*, pages 226–33.

19. Theorie der auf Konstante Spannung Erregten Wechselstrom-Fernleitung, **H. Piloty.** *Elektrotechnik und Maschinenbau*, volume 50, 1932, pages 54–8.

20. Elektrische Probleme der Energie-Uebertragung auf Grosse Entfernung, **R. Rüdenberg, Mitarbeiter.** *Transactions*, Second World Power Conference, volume 14, 1930, pages 110–40.

21. Hochleistungsuebertragung auf Weite Entfernung, **R. Rüdenberg, Mitarbeiter.** J. Springer, Germany, 1932.

22. Static-Stability Limits and the Intermediate-Condenser Station, **C. F. Wagner, R. D. Evans.** AIEE Transactions, volume 47, 1928, pages 94–121.

23. Progress in the Study of System Stability, **I. H. Summers, J. B. McClure.** AIEE Transactions, volume 49, 1930, pages 132–58.

24. Les Limites de Transport de l'Énergie Électrique à l'Aide du Courant Triphasé au Point de Vue de la Stabilité, **E. A. Tchernycheva, G. A. Lavrov.** International Conference on Large High-Voltage Systems (CIGRE), Paris, 1935, volume 1, report 123.

25. Transmission-Line Engineering, **W. W. Lewis.** McGraw-Hill Book Company, New York, N. Y., 1928.

26. La Ligne de Transport d'Énergie à 220 Kv de Kembs à Creney, Près Troyes, de la Société Transélectrique. *Transélectrique Revue Générale d'Electricité*, volume 36, 1934, pages 657–71.

27. Fonctionnement de la Ligne à 220 Kv de Kembs à Creney, Société Transélectrique *Transélectrique Revue Générale d'Electricité*, volume 41, 1937, pages 85–7.

28. Stability Limitations of Long-Distance A-C Power-Transmission Systems, **Edith Clarke, S. B. Crary.** AIEE Transactions, volume 60, 1941, pages 1051–9; discussion, pages 1299–1303.

Emergency Overloading of Air-Cooled Oil-Immersed Power Transformers by Hot-Spot Temperatures

V. M. MONTSINGER
FELLOW AIEE

P. M. KETCHUM
ASSOCIATE AIEE

Synopsis: The paper points out that during the period of the war the need for obtaining maximum transformer overload capacity is very great, particularly under rare emergency conditions. From a thermal standpoint, the amount of overload is limited by the hottest spot in the transformer winding. To obtain accurate data

1. On the actual hot-spot temperatures under rated load conditions

2. On the increase of the hot spot with load under both ultimate moderate overload and heavy short-time overload conditions

approximately 50 heat runs were made on several power transformers of different ratings, representing a wide variety of design conditions. The results of the tests are given in the paper.

The subject of how heavy overloads affect the life of transformers, is discussed and temperature limits somewhat higher than those given in the American Standards, Guides for Operation of Transformers, are suggested. Calculated short-time overloads are given to show the effect of transformer characteristics on overload values.

THE subject of overloading power transformers has been discussed in several AIEE papers,[1-3] and the American Standard *C*-57 gives recommendations on overloading transformers continuously in cool ambients, and in applying short-time overloads under recurrent and emergency conditions.

When the recommendations were first drawn up by the AIEE transformer subcommittee, considerable conservatism was used in the curves for short-time overloading, for two reasons:

1. It was a departure from a long-standing practice of permitting no overloading, a practice based on the assumption that temperature was the only factor that caused the aging of insulation. That is, "time" had not yet been recognized as an important factor. In fact, until 1922 the AIEE Standard stated that the name-plate rating must not be exceeded, "whatever be the ambient," and 105 degrees centigrade was considered the ceiling for hottest[a] spot temperatures for class *A* insulation until as late as 1934 or 1935.

2. The overload curves were purposely made safe for transformers having the highest hot spots under overload conditions.

While the practice of overloading transformers has been on the increase during the past few years, the importance of taking advantage of the maximum overload capacity has never been so great as it is today. It is important in two ways:

1. In many cases the demand for additional electric power has caught up with or even exceeded the present transformer capacity—when operated in the usual manner—and additional capacity is needed. A great many loading curves show that the load factor of most power transformers ranges from 50 to 65 per cent. A large reservoir of capacity is therefore available which could be used without causing a serious shortening of transformer life. This problem was discussed by one of the authors in 1940.[3]

2. We now have the important problem of taking care of rare emergency conditions caused either by electrical failure or by a saboteur. In the past it has generally been considered necessary to carry a spare transformer or a spare bank of transformers to pick up the load in case of a failure. Today many users are considering the use of, and are relying on, the short-time overload capacity of their transformers to carry them through rare emergency conditions. In fact, under some conditions it may be economical and necessary to use up 25 per cent, 50 per cent, or more of the transformer life to prevent a shutdown. To do this intelligently requires reliable information on how to calculate hot-spot temperatures and how the life of the transformer is affected by heavy overloads. The purpose of this paper is to discuss this problem.

I. Hot-Spot Temperatures in Transformers

Heat Runs Made

Since the rate of the deterioration of insulation in a transformer is governed by the hot-spot temperature in the winding, it is quite important to have accurate data on:

1. The value of the hot-spot temperature in various classes of transformers at rated load.

2. A practical method of calculating the hot-spot temperature with changes in the load under both ultimate and short-rime overload conditions.

To obtain such data, approximately 50 heat runs were made during the past year on three transformers of different ratings, one of which was operated

(*a*). As self-cooled.

(*b*). As forced-air-cooled.

The transformers were of the following ratings:

1. *H*-60-2,000 kva—34,500-volt class.

2. *H*-60-2,500 kva—34,500-volt class.

3. OAP[b]-60-2,500/3,125 kva—50,000-volt class.

Some of these tests were made using the loading-back (bucking) method, and some using the short-circuit method, to supply the losses.

Measurement of Hot-Spot Temperatures

Before the heat runs were made on the above transformers, a series of tests was made on a few coils in which thermocouples were embedded between turns to determine the most practical method of measuring the hot-spot temperature, since it is not practical to insert thermocouples between the turns of transformer windings. Figure 1 gives time-temperature curves for three heat runs with 1,500, 2,240, and 4,510 amperes per square inch. These tests showed that a thermocouple inserted under a spacer and in contact with the top surface of a horizontal coil represented very closely the hot spot in that coil. The coil second from the top was used for hot-spot measurements in each of the transformers mentioned above. No extra insulation, such as tape often placed on buffer coils, was used on these coils.

Effect of Change in Viscosity of Oil on Temperature Rise

To determine the effect of changes in viscosity of the oil with temperature on the winding rise over adjacent oil, several tests holding a constant loss were made on the previously mentioned small coils immersed in oil, whose temperature ranged from 35 to 100 degrees centigrade. The curves shown in Figure 2 were derived from these test data, together with the data given in Figure 21b, page 314, of reference 4. These curves are in close

---

[a] Referred to hereafter as "hot spot."

Paper 42-115, recommended by the AIEE committee on electrical machinery for presentation at the AIEE summer convention, Chicago, Ill., June 22-26, 1942. Manuscript submitted April 13, 1942; made available for printing May 19, 1942.

V. M. MONTSINGER is research engineer and P. M. KETCHUM is in the power transformer engineering department, both with General Electric Company, Pittsfield, Mass.

[b] Oil-immersed air pressure.

agreement with similar data given in reference 5.

Determination of Average-Winding Rise at Shutdown

All initial winding temperatures were obtained by cooling curves taken for at least ten minutes' duration. A comparison of the values tested and values calculated by the American Standards Association method (based on Figure 13 of reference 8) showed exceptionally close agreement, being within 0.1 to 0.5 degree centigrade in practically all cases. Extreme care was taken to obtain accurate cold winding temperatures.

Determination of Hot-Spot Temperatures

The determination of hot-spot temperatures in transformers either by tests or by calculation, must be considered in two stages:

1. Oil rise over ambient.
2. Hot-spot rise over oil.

The first stage is easier to determine than the second stage. The principal purpose in making the tests was to investigate the second stage—the hot-spot rise over oil and its variation with load.

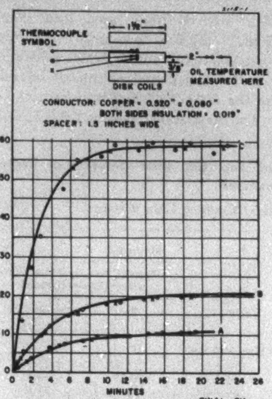
Methods have been available[4,7] for several years for calculating either ultimate or transient oil rises. That is, for ultimate conditions $\theta = KW^n$ where $n = 0.8$, and for transient conditions,

$$\theta = \theta_u\left(1 - e^{-\frac{T}{B}}\right) \tag{1}$$

Where

θ = oil rise in degrees centigrade
θ_u = ultimate rise, degrees centigrade
$\epsilon = 2.718$
T = time in hours
B = time constant[c] expressed in hours
$= \dfrac{\theta_u C}{W}$
$C = \dfrac{3.5 \text{ lb (copper+core+}^2/_3 \text{ tank)} + 90\, G}{60}$
W = loss in watts
G = gallons oil (U. S.)

There are two points, however, in the calculation of transient oil rises that need mentioning. When using equation 1, it was found that the calculated oil rises checked quite well the test rises obtained by the loading-back method, but did not check the test values obtained by the short-circuit method of loading. The principal difference in the two methods of loading is, of course, that in the loading-back method the temperature of the core keeps well ahead of the temperature of the oil due to excitation losses. On the other hand, in the short-circuit method, the temperature of the core lags the oil temperature. It was found that if the core weight was not used, the calculated values checked quite well the short-circuit test values. For example: for the OAP-60–2,500/3,125 kva size tested, $B = 3.7$ when the core weight was included, and 3.0 when the core weight was omitted. The calculated and tested top oil rises using the two methods of loading, are shown in Table I.



RUN	AMPS./SQ.IN.	θ_u °C	B*	FINAL OIL TEMP. °C
A	1500	10.5	5.08	75
B	2240	20.5	4.30	67
C	4510	59.0	2.72	86

TIME CONSTANT CALCULATED FROM θ_u AND LOSS.
SOLID LINES SHOW CALCULATED RISE.

Figure 1. Hot-spot exploration tests in disk coils

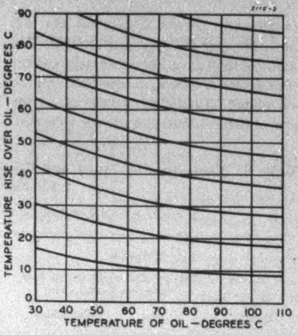


Figure 2. Effect of viscosity on temperature rise of transformer windings

These curves are for 10-C transformer oil and constant loss. The values ranging from 25 to 70 degrees centigrade oil temperature are based on Figure 21b, page 314, of reference 4. From 70 to 110 degrees centigrade, the curves are based on tests made with coils shown in Figure 1 of this paper

Table I. Comparison of Tested and Calculated Top-Oil Rise of the OAP-60–2,500/3,125-Kva Transformer

Loading-Back Run		
	Rise of Top Oil Over Ambient (Degrees Centigrade)	
Hours	Test	Calculated
		$B = 3.7$
1	8.3	8.1
2	14.3	14.2
3	19.4	18.9
4	23.3	22.4
6	28.4	27.2
10	32.5	31.6
Ultimate	33.9	33.9

Short-Circuit Run			
	Rise of Top Oil Over Ambient (Degrees Centigrade)		
Hours	Test	Calculated	
		$B = 3.0$*	$B = 3.7$
0.75	10.9	9.4	7.8
1.75	19.9	18.9	16.2
2.75	25.8	25.6	22.6
3.75	30.1	30.2	27.3
5.75	35.2	36.6	33.8
Load interrupted at this point			
Ultimate	42.9	42.9	42.9

* Core weight not used in deriving this value.

Short-circuit runs are, of course, of no importance under service conditions. They are discussed here, because it is more convenient to use this method when making heat runs (*a*) to measure hot-spot temperatures, and (*b*) to obtain transient oil-rise data (sometimes requested by customers); and it is well to remember that equation 1 should be modified (by omitting core weight) when calculating the oil rise. This same modification has been found necessary in other cases where the short-circuit method was used to obtain transient oil rise data.

The other point found was that for heavy short-time overloads the best results are obtained by using the time constant corresponding to that for ultimate rated load, although theoretically it should be based on ultimate (calculated) conditions using the initial loss in

$$B = \frac{\theta_u C}{\text{loss}}$$

and the final oil rise θ_u for the ultimate loss corresponding to the final winding temperature. While this method can be used for moderate overloads as was done in reference 7, it did not give even approximately correct results for the 200 per cent and 270 per cent loads. The method that gave approximately correct results was to base θ_u in equation 1 on the calculated ultimate rise for the loss at the end of the overload and use the time constant derived from rated load conditions. This was done for the two overloads shown in Table II, which gives comparative tested and calculated oil rises for the *H*-60-2,500-kva transformer.

---

[c] Time to reach 63 per cent of the ultimate rise, or time to reach ultimate rise if all heat is stored.

The calculations given in Table II show that fairly accurate results were obtained using the rated load (total loss) time constant of 3.17, and indicate that results close enough for practical purposes can be obtained by the simpler method 1 instead of method 2 (described in the footnote). Heat runs made on other transformers confirmed these findings. However, if the initial rise is changing (that is, not ultimate for a given loss) and is well along—over 30 to 40 per cent of the ultimate rise—method 2 should be used. The use of the same (rated load) time constant simplifies the calculations of transient oil rises for short-time overloads.

Table II. Comparison of Tested and Calculated Top Oil Rises—Short-Circuit Runs—2,500-Kva Transformer

B=3.17—Time Constant Computed by Omitting Core Weight

	Method 1			Method 2	
	Top-Oil Rise Over Initial Rise (Degrees Centigrade)			Top Oil Rise Over Ambient (Degrees Centigrade)	
Time (Hours)	Test	Calculated	Time (Hours)	Test	Calculated*
Total Loss Run (Final Loss 26.9 Kw)					
0	0.0	0.0	0+1.0	11.9	11.9
2	14.8	15.0	2+1.0	26.7	26.7
4	22.0	22.6	4+1.0	33.9	34.6
6	27.0	27.0	6+1.0	38.9	38.8
8	29.4	29.2	8+1.0	41.3	41.0
10	31,0	30.4	10+1.0	42.9	42.4
Ultimate	31.8**	31.8	Ultimate	43.7	43.7
			Time to reach 11.9 C rise=1.0 hour		
200 Per Cent Rated Load Current Run (Final Loss 91 Kw)					
0	0.0	0.0	0+0.35	12.0	12.0
1	25.3	27.9	1+0.35	37.3	40.4
2	46.7	48.5	2+0.35	58.7	60.4
2.75	59.3	59.7	2.75+0.35	71.3	72.1
Ultimate (Calculated)		103.0	Ultimate (Calculated)		115.0
			Time to reach 12.0 C rise=0.35 hour		
Calculated final oil rise=115 C. Initial oil rise=12.0 C. θ_u=115.0−12.0=103.0 C for method 1, and 115 C for method 2.					
270 Per Cent Rated Load Current Run (Final Loss 170 Kw)					
0	0.0	0.0	0+0.36,	20.3	20.3
0.50	23.1	25.1	0.50+0.36	43.4	45.2
0.92	40.4	42.7	0.92+0.36	60.7	63.9
Ultimate (Calculated)		169.7	Ultimate (Calculated)		190.0
			Time to reach 20.3 C rise=0.36 hour		
Calculated final oil rise=190 C without resistance correction. Initial oil rise=20.3 C. θ_u=190.0−20.3=169.7 C for method 1 and 190 C for method 2.					

* Calculated by finding time from Figure 3 to attain rise over ambient at start of overload and then continuing from this time on to end of overload. For example: for the total loss runs, it would require one hour to reach 11.9 degrees centigrade rise. One hour is then added to each succeeding time period and 43.7 degrees centigrade is used for θ_u instead of 31.8 degrees centigrade.

** 43.7−11.9=31.8

Table III. Summary of Heat Runs Made on *H*-60-2,000-Kva Transformer

Loading-Back Method Used to Supply Losses

	Method of Cooling							
	Self-Cooled					Forced-Air-Pressure (OAP)		
	Per Cent of Self-Cooled Rating							
	100	125	150	200	250	100 133	125 166.5	150 (OAP) 200
Hot-spot temperature	82.5	97.0	114.0	130.0	137.0	85.7	110.1	133.2
Hot-spot temperature (calculated)*	84.0	98.8	116.1	132.0	134.0	87.7	113.0	138.0
Top oil	73.5	83.3	94.6	97.0	84.0	71.1	88.2	102.8
Hot-spot rise over top oil	9.0	13.7	19.4	33.0	53.0	14.6	21.9	30.4
Average difference top and bottom tube header (outside surface)	12.4	14.4	14.4	15.0	15.8	12.4	12.4	14.8
Average winding temperature by resistance	77.6	90.1	104.9	115.2	116.9	79.5	99.9	120.2
Ambient (degrees centigrade)	24.5	23.3	26.5	31.7	28.0	26.5	32.3	30.9
Average high-voltage rise over ambient	53.1					53.0		
Hot-spot rise over average winding rise	4.9	6.9	9.1	14.8	20.3	6.2	10.2	13.0
Average high-voltage rise over average oil temperature	10.3	14.0	17.5	25.7	48.8	14.6	17.9	24.7
Relative copper loss (corrected for temperature)	1.0	1.63	2.45	4.48	7.03	1.78	2.95	4.52
Duration of test	Ultimate	3.5 hr	3 hr	46 min	23.5 min	Ultimate	3 hr 22 min	3 hr 34 min

* By using calculated hot-spot rise over top oil for self-cooled rated load and then varying rise over top oil as loss[8,9].

Determining Hot-Spot Rise Over Oil

The determination of the actual hot-spot rise over the oil (and over the average winding temperature) at rated load for different design conditions, and the variation of the hot spot with load, involves several factors not so easy to determine as they are for oil temperature rises. Among these are:

1. Vertical oil gradient as affected by the center of heat[d] with respect to the center of the cooling circuit (radiators, tube headers, and so forth).

2. Accurate determination of the oil temperature adjacent to that part of the winding having the maximum temperature rise.

3. Effect of blanketing of

(*a*). Part of coil area by spacers.

(*b*). Total coil area by tape.

(*c*). Extra turn insulation often used in buffer coils

4. Effect of change in viscosity of oil and change in resistance with temperature—minor factors, as one tends to counteract the other.

5. Transient temperature rise (when the duration of time is under 15 to 20 minutes).

As previously mentioned, it was for the purpose of obtaining more accurate data on these factors (than have been available heretofore) that tests were made during 1941 on five different design conditions—three different designs, one of which was tested under three different vertical oil gradient conditions.

First Series of Heat Runs (Loading-Back Method)

Figure 4 shows the location of thermocouples in the *H*-60-2,000-kva transformer tested first, as self-cooled, and second, as OAP-2,660-kva with temporarily mounted fans. In both series of tests 53 degrees centigrade high-voltage winding rise was produced by blanketing some of the tube headers. To protect the ther-

---

[d] Usually a point half-way up the coils and core.

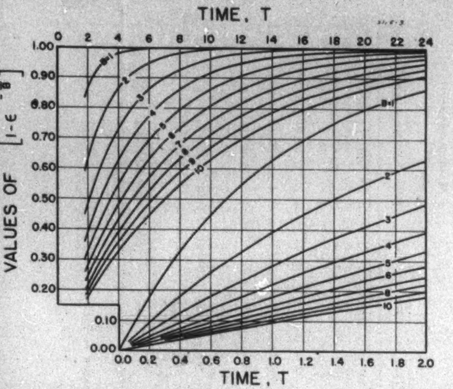


Figure 3. Transient heating curves

mocouples placed under the spacers resting on the next to top coil from dangerous voltages, the coil crossover at this point was grounded during the tests. Figure 4 also shows the vertical temperature gradient of the oil for the 100, 125, and 200 per cent load self-cooled runs. The OAP oil gradients were essentially of the same shape.

Table III gives the final temperature readings. Figure 5 shows in curve form the hot-spot rise:

1. Over average winding temperature.
2. Over top oil.
3. Over high-voltage oil duct temperature.
4. Average winding rise over average oil temperature.

These data show:

1. The hot-spot rise over average winding temperature was between four and five degrees at rated load and varied as loss$^{0.75}$.
2. The hot-spot rise over top oil (and over maximum observed oil) varied as loss raised to approximately 0.8 power.
3. The average winding rise over average oil temperature[ε] varied as loss$^{0.7}$.
4. The temperature throughout the main body of oil above the top of the core was approximately constant.

Second Series of Heat Runs (Short-Circuit Method)

The second series of heat runs was made on the transformer rated *H*-60–2,500 kva under three different design conditions, namely:

1. With core and coils resting on bottom of tank—"normal position."
2. With center of heat (half-way up core and coils) raised to be level with center of cooling tubes—"maximum raised position."
3. With center of heat in "intermediate position."

Figure 6 shows the locations of the windings, thermocouples, and vertical oil gradients for each of the three conditions tested. It will be noted that raising the core and coils increased the vertical oil gradients, these being indicated in Table A.

Table A

Position of Core and Coils	Difference Between Top and Bottom Oil Inside Tank (Degrees Centigrade)
Normal	15.0
Intermediate	25.0
Maximum raised	40.0

Figures 7, 8, and 9 show the oil and winding rises for this series of tests. Curve *E* shows the effect of raising the center of heat on the hot-spot rise over the average winding temperature. At rated load the rises, as read from the curves, were:

(*a*). 5.1 degrees centigrade for the normal position.

(*b*). 14 degrees centigrade for the intermediate position.

(*c*). 18 degrees centigrade for the maximum raised position.

It is, of course, proper to point out that most power transformers fall in the (*a*) condition where the center of heat is well below the center of cooling tubes. The (*c*) condition, where the center of heat and center of cooling tubes are at the same level, is one that seldom exists in practice.

Table IV gives the final readings for 20 of the 32 runs made. The 12 additional tests were check runs of two to three hours' duration, made only for obtaining winding temperatures, and were not run until the top oil rises again became constant.

The Thermal Circuit

The thermal circuit in a self-cooled transformer is composed of the tempera-

---

[ε] The average oil temperature is taken as the top oil minus one-half the temperature drop through the cooling tubes.

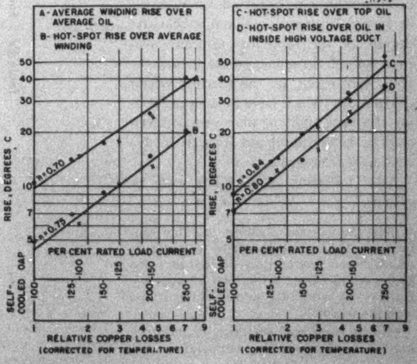


Figure 5. Temperature rises during loading-back heat runs on *H*-60–2,000-kva transformer

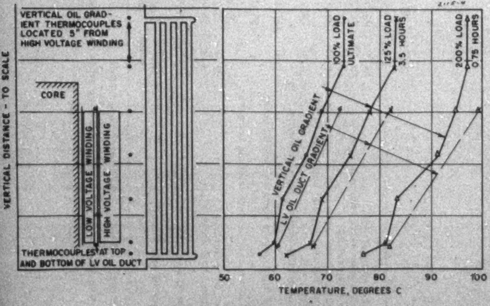


Figure 4. Temperature gradients during loading-back heat runs on *H*-60–2,000-kva transformer

ture rise of the oil flowing upward through the coil stack and the temperature drop in the oil flowing downward through the attached tube headers or radiators.

The height of the center of heat (center line of core and coils, or of coils only, for short-circuit runs) with respect to the center of the cooling agent, controls the temperature rise and temperature drop of the thermal circuit for ultimate conditions. Also, when the center of heat is well below the center of the cooling agent, the temperature rise and temperature drop in the thermal circuit are small and do not appreciably increase within a reasonable range of loads for ultimate conditions. But, for short-time heavy overloads, while the temperature drop in the cooling circuit may not appreciably increase in value, the temperature rise of the oil flowing upward may greatly increase. For example: for the normal position, there was no appreciable change for all the ultimate runs in either the temperature rise or temperature drop of the thermal circuit. On the other hand, for the 200 per cent and 270 per cent short-time runs, the temperature drop in the cooling tubes remained essentially the same as for the ultimate runs—while the temperature rise of the oil flowing up through the windings increased approximately 100 per cent over the rise for the ultimate runs. This increase in the oil temperature flowing upward increased the hot-spot rise over the average winding temperature, Figure 7, whereas, if the constant temperature drop in the cooling tubes had been the controlling factor, the hot spot would not have increased with load.

When the center of heat was raised, both the temperature rise and the temperature drop in the thermal circuit definitely increased with load under both ultimate and short-time overload conditions. The temperature drops in oil flowing down through the tube headers, shown in Figure 10, were predicted within one degree for the total loss ultimate heat runs for all three conditions before the tests were made, by the thermal circuit calculations (which have been in use during the past eight or nine years) developed by L. Wetherill and given in his discussion of reference 5.

The following conclusions can be stated:

1. For the normal position, the hot-spot rise over the average winding temperature was between five and six degrees at rated load and increased with load at approximately the same rate as the hot-spot rise over the top oil.

For the two raised positions, the hot-spot rise over average winding temperature defi-

Table IV. Summary of Heat Runs Made on *H*-60–2,500-Kva Transformer, Short-Circuit Method Used to Supply Losses

	Position of Core and Coils																		
	Normal								Intermediate						Maximum Raised				
	Load Current (Per Cent of Rating)																		
	58	85	100	112*	125	135	200	270	57	85	112*	135	200	270	85	100	112*	125	200
Hot-spot temperature	43.5	63.7	78.0	85.9	93.3	107.9	128.0	138.0	47.6	65.8	93.7	104.9	107.3	133.3	77.5	93.3	105.7	114.5	125.5
Hot-spot temperature (calculated)**	42.8	62.7	78.6	85.7	93.9	110.0	134.6	145.9	47.8	64.9	92.7	106.2	107.4	134.2	75.0	90.0	98.0	112.4	122.5
Top oil temperature	37.3	54.5	67.2	72.5	78.5	91.6	99.4	87.8	39.8	52.4	75.1	84.4	73.9	87.7	64.4	77.7	87.3	96.4	95.1
Hot-spot rise over top oil	6.2	9.2	10.8	13.4	14.8	16.3	28.6	50.2	7.8	13.4	18.6	20.5	33.4	45.6	13.1	15.6	18.4	18.1	30.4
Difference between top and bottom oil in tube headers	5.4	8.9	9.0	8.5	10.0	10.0	10.6	12.6	10.3	14.9	18.5	21.8	22.3	27.1	21.1	27.5	32.9	34.4	42.9
Temperature rise of oil up through low voltage	7.4	11.1	10.5	11.2	12.1	12.0	20.0	27.6	10.3	18.2	21.0	24.1	33.9	48.8	25.3	33.1	38.7	40.0	66.1
Average high-voltage temperature (by resistance)	—	58.6	—	—	86.2	—	117.2	119.3	40.8	53.3	78.3	89.2	92.2	110.4	64.4	74.6	82.7	91.4	99.0
Hot-spot rise over average winding temperature	—	5.1	—	—	7.1	—	10.8	18.7	6.8	12.5	15.4	15.7	15.1	22.9	13.1	18.7	23.0	23.1	26.5
Ambient temperature	22.4	25.7	28.7	28.8	26.8	30.0	28.1	27.1	22.6	22.3	25.1	23.0	22.5	24.4	28.1	30.4	29.2	29.6	22.8
Top oil rise over ambient	14.9	28.8	38.5	43.7	51.7	61.6	Not constant		17.2	30.1	50.0	61.4	Not constant		36.3	47.3	58.1	68.8	Not constant
Average oil rise over ambient	12.2	24.3	34.0	39.4	46.7	56.6	Not constant		12.0	22.6	40.8	50.5	Not constant		25.7	33.5	41.6	49.6	Not constant
Duration of test (hours)	Ultimate	Ultimate	Ultimate	Ultimate	Ultimate	Ultimate	2.75	0.92	Ultimate	Ultimate	Ultimate	Ultimate	0.92	0.92	Ultimate	Ultimate	Ultimate	Ultimate	2.08

*112 per cent load loss = total (core + copper) loss.

**By using calculated "hot-spot rise over top oil at rated load and then varying rise as loss" where $n = 0.8$ for normal position, and 0.53 for intermediate and maximum raised positions.

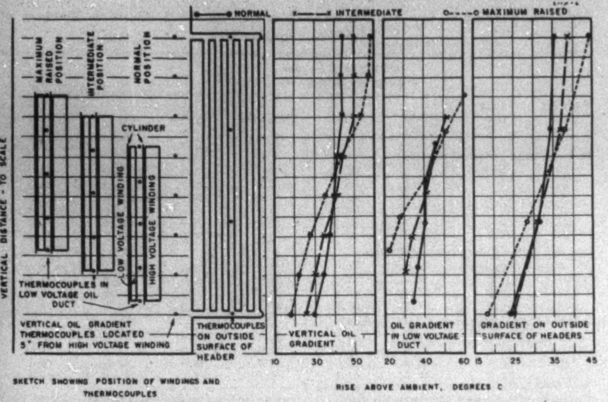


Figure 6. Temperature gradients during short-circuit heat runs on *H*-60–2,500-kva transformer

nitely increased with load for all ultimate conditions, but under short-time overload conditions, the hot-spot rise over average winding was only slightly above the rises for the 135 per cent load ultimate run—see Figures 8 and 9.

2. The ultimate average and top oil rises varied as loss raised to a power ranging from approximately 0.75 to 0.85—the general average being about 0.8.

3. The difference between the top and bottom temperature of the oil in the cooling tubes for ultimate conditions was approximately constant from 100 per cent to 135 per cent load for the normal position, but increased with loss raised to approximately 0.4 power for the intermediate position, and to approximately 0.7 power for the maximum raised position.

4. The hot-spot rise over top oil for both ultimate and short-time overloads varied as loss raised to a power of approximately 0.75 (test points scattered) for the normal position, and approximately as $\text{loss}^{0.53}$ for both intermediate and maximum raised positions.

5. The main body of oil above the windings (not core yoke) was at approximately the same temperature.

Third Series of Heat Runs

A third series of heat runs was made on an OAP-60–2,500/3,125-kva transformer in which the losses were supplied

1. By the short-circuit method.
2. By the loading-back method.

Only self-cooled runs were made. The main purpose of these tests was twofold:

1. To obtain comparative tests on short-circuit and loading-back heat runs to determine the effect of the loss in the core (top yoke) on the hot-spot rise over top oil.
2. To obtain additional data on the hot-spot rise over the average winding temperature for both transient and ultimate conditions.

The data for this series of tests are given in Table V, and some of the data are plotted in Figure 11. These data show:

1. The hot-spot rise over the average winding temperature was between five and six degrees at rated load, and increased as $\text{loss}^{0.8}$ for both the short-circuit and loading-back runs.

2. The hot-spot rise over top oil (when corrected to the same oil temperature) was approximately the same for both short-circuit and loading-back methods of loading. In other words, the short-circuit method of loading can be used to obtain hot-spot temperatures in transformers. The rise varies as $\text{loss}^{0.8}$.

3. The average winding rise over average oil varied as $\text{loss}^{0.7}$.

Variations of Hot Spot With Load

Vogel and Narbutovskih[5] reported that the hot-spot rise over average winding rise of a 600-kva transformer was constant from 100 per cent load to 250 per cent load inclusive. These constant hot-spot test values were explained on the grounds that this would be expected since the square root of the product of the losses and oil viscosity was a constant value, which in turn produced an approximately constant difference in the temperature of the top and bottom oil in the radiators. In the tests reported in this paper, Table VI shows the difference between top and bottom cooling tube temperatures and the square root of the product of the loss and oil viscosity for different loads held on the 2,500-kva transformer tested with the center of heat at different levels. These data show that with the core and coils in the normal position this factor was approximately constant except for the 58 per cent load test; but, for the intermediate and maximum raised positions of core and coils, there

Table V. Summary of Self-Cooled Heat Runs Made on OAP-60–2,500/3,125-Kva Transformer

	Test Readings, Degrees Centigrade										
	Short-Circuit Heat Runs						Loading-Back Heat Runs				
	Load in Per Cent of Rating										
	48.6	81.1	101.0	121.3	141.8	228.0	48.6	80.8	99.0	122.0	140.0
Hot-spot temperature	29.2	50.6	61.2	74.8	91.6	125.8	42.7	60.9	69.9	78.6	102.7
Hot-spot temperature (calculated*)	26.3	51.5	64.1	75.3	92.0	134.4	45.6	60.8	73.0	76.4	102.6
Top oil temperature	21.5	40.3	45.9	53.8	62.6	69.4	41.7	51.9	57.3	58.6	79.5
Hot-spot rise over top oil	7.7	10.3	15.3	21.0	29.0	56.4	1.0	8.2	12.6	20.0	23.2
Hot-spot rise over top oil (all rises corrected to 75-degree oil temperature Figure 2)	—	2.9?	8.8	16.1	26.5	55.2	—	3.8	9.1	16.3	24.0
Difference between top and bottom tube headers	4.5	10.8	12.5	15.2	15.9	21.3	10.7	11.6	12.8	14.2	15.6
Average winding temperature	26.5	47.6	55.6	64.7	77.4	108.9	41.6	57.2	64.7	—	93.4
Hot-spot rise over average winding temperature	2.7	3.0	5.6	10.1	14.2	16.9	1.1	3.7	5.2	—	9.3
Average winding rise over average oil temperature	7.2	12.7	15.9	18.5	22.7	50.1	5.2	11.1	13.0	—	51.4
Copper loss (kw)	3.17	9.23	13.62	20.8	30.6	83.0	3.2	9.0	13.8	21.3	29.4
Core loss (kw)	0	0	0	0	0	0	6.13	6.13	6.13	6.13	6.13
Total loss (kw)	3.17	9.23	13.62	20.8	30.6	83.0	9.33	15.13	19.93	27.43	35.53
Duration of run (hours)	3	3	3	3	3	1.1	8.5	6	14.7	2	12.5

* By using calculated hot-spot rise over top oil at rated load and then varying this rise as $\text{loss}^{0.8}$.

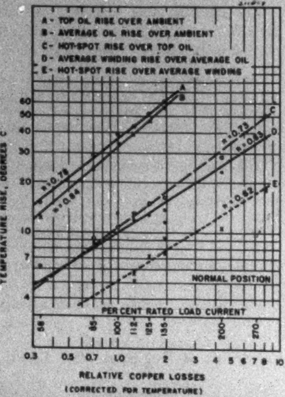


Figure 7. Temperature rises during short-circuit heat runs on *H*-60–2,500-kva transformer

Table VI. Product of Loss and Kinematic Viscosity for Three Different Positions of 2,500-Kva Transformer

Per Cent Load	Loss (Kw)	Oil Inside of Header (Degrees Centigrade)			$\sqrt{\text{Loss}\times\text{Kinematic Viscosity}}$*		
		Top	Bottom	Difference	Top Oil	Avg. Oil	Bottom Oil
Core and Coils—(Normal Position)							
58	6.975	37.2	31.8	5.4	26.4	29.0	29.2
85	14.7	54.4	45.5	8.9	29.7	31.6	34.0
100	20.9	67.0	58.0	9.0	29.7	31.4	33.6
112	26.9	72.5	64.0	8.5	31.4	33.0	35.0
125	32.3	78.7	68.7	10.0	31.6	33.6	35.8
135	40.4	90.8	80.8	10.0	29.5	32.0	34.2
Core and Coils—(Intermediate Position)							
57	6.56	40.7	30.4	10.3	24.4	26.3	28.6
85	14.7	52.2	37.0	15.2	30.6	34.3	38.7
112	26.9	75.4	56.9	18.5	30.0	34.2	38.7
135	40.4	84.8	63.0	21.8	32.0	37.5	43.5
Core and Coils—(Maximum Raised Position)							
85	14.7	64.1	43.0	21.1	25.8	30.0	35.4
100	20.9	77.0	49.5	28.5	26.0	31.0	38.2
112	26.9	86.8	53.9	32.9	26.0	32.5	40.4
123	32.3	95.7	61.3	34.4	27.7	35.4	44.4

*Curve given in Figure 2 of reference 5 used. This curve represents a fairly universal oil.

was a constant increase with load. Yet, the hot-spot rise over the average winding temperature increased with load for all ultimate conditions in all three positions. The temperature drops in the tube headers are plotted in curve form in Figure 10.

The procedure to follow in the general case for calculating hot-spot temperatures with windings similar to those in the transformers tested, is illustrated in the following example.

These assumptions are made:

1. 55 degrees centigrade average winding rise at rated load.
2. 45 degrees centigrade top oil rise at rated load.
3. 10 degrees drop in cooling circuit at rated load.
4. 5 degrees hot-spot increase due to extra coil insulation, spacers, and so forth.

If located at the top the hot-spot rise of the extra insulated coil would be

$$55+\frac{10}{2}+5=65 \text{ degrees centigrade}$$

Under overload conditions after 15 minutes' duration, hot-spot rise would be:

$$A+B\left(\frac{\text{loss at overload}}{\text{loss at rated load}}\right)^{0.8}$$

where

A = temperature rise of the top oil
B = difference between the hot-spot and top oil at rated load

Factors Limiting Short-Time Rare Emergency Overloads

Quite often factors other than the life of the insulation limit heavy overloads for transformers. Among such factors to be considered are, contacts, leads, joints, and oil. Effect of heavy overloads on regulation must, of course, be carefully considered.

Excessive expansion may cause oil to run over. Also, oil deteriorates at high temperatures even for short periods of time, particularly if exposed to air. Figure 12 shows calculated hot-spot and top oil rises for a four-hour 200 per cent load following full load, in a case studied recently. In this case the temperature of the oil was considered the limiting factor.

Effect of Change in Resistance and Oil Viscosity on Hot-Spot Temperatures

Figure 12 also shows the difference in the hot-spot rises obtained when no temperature and viscosity corrections are made as compared with the rises obtained with the corrections. The calculations for 200 per cent load (assuming 20 degrees centigrade hot-spot rise over top oil at 100 per cent load) were made as follows:

1. With No Corrections

Hot-spot rise over oil $=20\times2^{1.6}=60.4$ degrees centigrade after 20 minutes' duration

Hot-spot temperature = top oil + 60.4 degrees centigrade for any time after 20 minutes

2. With Corrections

For $2\,{}^3/_4$ hours, oil temperature = 110 degrees centigrade (by Figure 12)
Hot-spot rise over oil =

$$20\left[2^2\times\left(\frac{234.5+177.5}{234.5+95}\right)\right]^{0.8}=72.5 \text{ degrees}$$

centigrade with resistance correction only

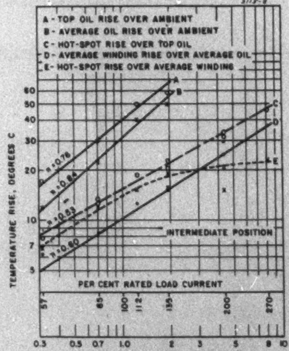


Figure 8. Temperature rises during short-circuit heat runs on *H*-60–2,500-kva transformer

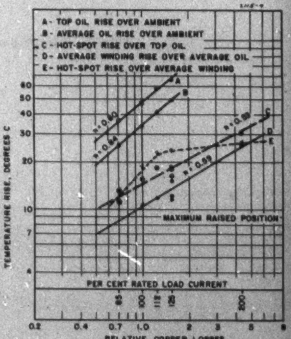


Figure 9. Temperature rises during short-circuit heat runs on *H*-60–2,500-kva transformer

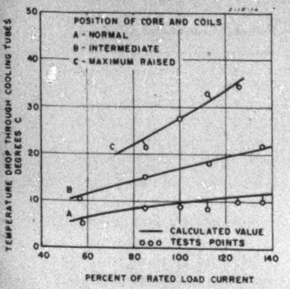


Figure 10. Cooling tube temperatures during short-circuit heat runs on *H*-60-2,500-kva transformer

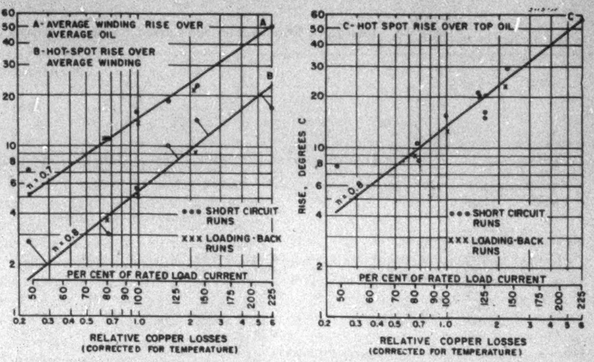


Figure 11. Temperature rises during heat runs on OAP-60-2,500/3,125-kva transformer

By Figure 2 if the hot-spot rise is 72.5 in 75 degrees centigrade oil, it would be 67.5 degrees centigrade rise in 110 degrees centigrade oil—a reduction of 5 degrees centigrade due to viscosity correction.

The correct hot-spot temperature is, therefore, 110+67.5=177.5 at $2^{3}/_{4}$ hours, as against 110+60.4=170.4 degrees centigrade without the correction.

These curves show that for strictly accurate results both corrections should be made. But, since in making overload calculations the bases of the assumptions are approximations, it should be satisfactory to neglect the corrections. This was done when calculating the overloads shown in Table VII.

Hot-Spot Temperature Indicator Tests

Temperature indicators have been used for several years to indicate hot-spot temperatures in transformers. Since an indicator can be set for any desired hot-spot rise over average winding at rated load, its ability to follow the hot-spot temperature depends

1. On its variation with load current.
2. On the time constant of its heating coil.

To check these variations with load current, a standard indicator heating coil was placed in the top oil of both the 2,000-kva self-cooled /2,667-kva OAP and the 2,500-kva self-cooled transformer.

Figure 13 shows that on the average the indicator rise over top oil varied as the load current$^{1.70}$ or as loss$^{0.85}$, whereas (on the average) the winding hot-spot rise over top oil varies as loss$^{0.8}$. This means that for heavy overloads the indicator will read a little too high, and it may be well to retain this feature. The time constant of the standard indicator heating coil is approximately the same as the time constant of transformer windings; consequently the indicator should follow the hot-spot temperature under both fluctuating loads and overloads.

General Conclusions on Steady-State and Transient Temperature Rises

1. For rated load conditions of self-cooled power transformers, the hot-spot rise over average winding temperature by resistance ranges from approximately five degrees centigrade upward. For moderate size and voltage rating (below approximately 50 kv), it naturally comes between five and six degrees. But, for large size and high-voltage rating, steps have to be taken by the designer to limit the hot-spot to ten degrees centigrade above average.

2. For transformers having an OAP rating 1.33 times the self-cooled rating, the hot-spot rise over average winding temperature is approximately 40 per cent greater than that for the self-cooled rating (see Table III).

3. The hot-spot rise over average winding temperature increases with load (for both ultimate and short-time overloads), but no definite rate of rise versus loss can be stated (see Figures 7, 8, 9, and 11).

4. The average winding rise over average oil varies as the loss raised to a power ranging from 0.6 to 0.7.

5. The hot-spot rise over top oil varies as the loss raised to a power ranging from approximately 0.7 to 0.8, generally more nearly the 0.8 power for most transformers.

6. The oil time constant used in calculating the temperature rise of the oil for short-time overloads should be based on the ultimate rise and loss for rated load conditions.

7. The winding time constant used in calculating the temperature rise of windings over oil before conditions become constant, should be based on the calculated ultimate rise and loss for the load under consideration.

8. Either loading-back or short-circuit method of loading can be used to determine both hot-spot and average winding temperatures. When calculating the transient oil rises by equation 1, for the short-circuit method the weight of the core should not be used in deriving the time constant.

9. The temperature rise of both the average and top oil varies approximately as the loss$^{0.8}$ for ultimate conditions.

II. Effect of Overloads on Life of Transformers

Life Expectancy of Transformers

The estimated life of a transformer is at best only a rough approximation. The dielectric strength of insulation does not deteriorate until it has become embrittled and cracked. It is possible, therefore, for a transformer to continue to operate long after the mechanical life of its insulation is well used up, unless subjected to excessive mechanical stresses (short circuit, handling, or other mechanical shocks), since its normal stresses are quite low. It does not follow, therefore, that a transformer will fail when its insulation is embrittled; on the contrary, if severely strained, it might fail before the mechanical strength is completely used up.

Rate of Aging of Class-A Insulation

Mr. Clark's paper, presented at this session, shows that for temperatures ranging from approximately 115 to 200 degrees centigrade, the rate of aging is doubled for each six to ten degrees centigrade depending upon the percentage of life used up. That is, near the start a nine- to ten-degree rule applies; at the 50 per cent to 25 per cent life levels an eight-degree rule applies; and near the finish a six- to seven-degree rule applies.

Table VII. Emergency Overloads for Self-Cooled Power Transformers Having Different Characteristics*

		Transformer Class												
		1			2			3		4		5		6
		a	b	c	a	b	c	a	b	a	b	a	b	ASA
Hot-spot rise		60	60	60	62.5	62.5	62.5	60	60	65	65	65	65	
Top oil rise		50	50	50	50	50	50	45	45	50	50	45	45	
Time constant		3	5	7	3	5	7	3	5	3	5	3	5	
Hours	Temp. C	Times Rated Load Current												
Following Full Load														
1/4	152	3.00	3.13	3.16	2.67	2.81	2.84	2.46	2.54	2.42	2.51	2.12	2.18	1.8
1/2	144	2.53	2.76	2.81	2.30	2.50	2.53	2.19	2.27	2.13	2.23	1.91	1.97	1.6
1	136	2.10	2.28	2.40	1.95	2.10	2.13	1.88	1.98	1.82	1.94	1.68	1.76	1.4
2	128	1.70	1.86	1.97	1.62	1.77	1.82	1.60	1.69	1.54	1.64	1.47	1.52	1.3
4	120	1.45	1.55	1.62	1.40	1.47	1.53	1.41	1.46	1.33	1.40	1.30	1.35	1.2
8	115	1.32	1.35	1.40	1.28	1.31	1.34	1.32	1.35	1.24	1.27	1.23	1.25	1.15
24	110	1.26	1.26	1.26	1.21	1.21	1.21	1.24	1.24	1.18	1.18	1.17	1.17	1.10
Following No Load (18 Degrees Centigrade Oil Rise)														
1/4	152	3.70	3.94	4.06	3.31	3.50	3.60	3.04	3.13	2.96	3.11	2.62	2.68	2.3
1/2	144	3.10	3.43	3.60	2.85	3.08	3.20	2.67	2.83	2.62	2.80	2.33	2.45	1.9
1	136	2.51	2.78	3.04	2.35	2.60	2.78	2.27	2.46	2.20	2.40	2.03	2.17	1.6
2	128	1.95	2.25	2.45	1.86	2.10	2.29	1.84	2.05	1.77	1.98	1.68	1.84	1.5
4	120	1.57	1.76	1.92	1.50	1.68	1.82	1.52	1.67	1.46	1.58	1.41	1.54	1.35
8	115	1.35	1.44	1.54	1.31	1.39	1.48	1.33	1.40	1.27	1.34	1.26	1.32	1.25
24	110	1.26	1.26	1.26	1.21	1.21	1.21	1.24	1.24	1.18	1.18	1.17	1.17	1.10

* Based on top oil rise over ambient and hot-spot rise over top oil varying as $loss^{0.8}$. Loss ratio 2.5:1 at rated load.

No corrections made for increase in resistance and change in viscosity of oil with temperature. 30 C ambient.

In view of the fact that predicting the life of a transformer, as mentioned above, cannot be exact, the eight-degree rule, which one of the authors has used for several years,[1,3] is a good average value to use.

Mr. Clark's data show also that for temperatures from 75 to 100 degrees centigrade the rate of aging doubles with changes in temperature as low as two to five degrees centigrade. This indicates that it is not possible to use aging data for the higher temperatures and the eight-degree rule to predict the life of a transformer when operating at temperatures of approximately 100 degrees centigrade and below. In fact, Figures 3 and 4 of Mr. Clark's paper indicates that while the life of insulation is a matter of weeks at 120 degrees centigrade, it is a matter of several years if the temperature does not exceed approximately 100 degrees centigrade. While these laboratory tests may not show the actual life of a transformer in weeks and years, they are indicative of the relative life at different temperatures.

Basis for Determining the Life of Transformer Insulation

There is, of course, some question whether laboratory aging tests made on isolated strips of paper in sealed tubes can be applied directly in estimating the life of insulation in a transformer. Such tests should rather be depended upon to furnish the more fundamental data, such as the increase in temperature to double the rate of aging, the effect of the various other factors contributing to the deterioration of insulating materials, and so forth.

In attacking the problem of determining the effect of overloads on the life of transformers, it is felt that for the present it is preferable to rely mainly on past experience in establishing a basis, and then depend upon fundamental laboratory data as an additional aid. For example: we have now had several years' experience in the use of hot-spot temperature limits for emergency overload conditions, and the laboratory tests show how the relative life of insulation is affected by different temperatures.

The hot-spot temperature limits given in section 11.002 of the American Standards Guides for Operation of Transformers under emergency conditions are 115 degrees centigrade for 2 hours, 110 degrees centigrade for 8 hours, and 105 degrees centigrade for 24 hours. When these limits were first set up a few years ago, they were purposely made conservative for the reason that the permitting of overloads on an emergency basis was a departure from a long-standing practice of permitting no overloads, as pointed out in the first part of the paper. Furthermore, no limitations were put on the number of overloads during the life of the transformer.

When the number of operations is limited, it is reasonable to increase somewhat the above American Standards hot-spot limits. Using a rule less than eight degrees for temperatures under 120 degrees centigrade, an eight-degree rule for temperatures from 120 to 192 degrees centigrade, which are in agreement with Mr. Clark's data, and a rule greater than eight degrees above 192 degrees centigrade, in order to reach the present ASA limit of 250 degrees centigrade in five seconds, a schedule of temperatures is suggested in Table B for consideration in setting up emergency overloads.

The use of a rule greater than eight degrees above 192 degrees centigrade appears reasonable, since the actual temperatures (calculated on the basis of all heat stored in the copper) will generally be below the calculated values, for two reasons:

1. The insulation on the conductors will absorb some heat.
2. Most transformer windings start dissipating heat after from three to five seconds' duration.

The temperature limits suggested are in close agreement with values assumed in reference 5 for times of 1/4 hour and longer, being somewhat higher for the short times, and the same for 4, 8, and 24 hours' duration.

Calculation of Emergency Overloads

The overloads for a given hot-spot temperature vary widely, depending on the characteristics of the transformer, such as top oil rise, hot-spot rise over top oil, ratio of losses, and time constant, all of which vary under rated load conditions for power transformers, approximately as shown in Table C.

Based on the temperature limits sug-

Table B

Time, in seconds	5	10	20	30	60	300	
Temperature, degrees centigrade	250	220	200	192	184	165	
Time, in hours	1/4	1/2	1	2	4	8	24
Temperature, degrees centigrade	152	144	136	128	120	115	110

gested in Table C, Table VII gives the calculated times rated load current for 1/4 hour to 24 hours inclusive for self-cooled transformers having different characteristics. The present ASA emergency overloads are also shown for comparison. These calculated overloads are given principally for the purpose of showing how they vary for different transformer characteristics. It will, of course, be possible to reduce a group of this kind into three or four classes, since, in several cases, the overload values are of the same order of magnitude. Similar overload values can be calculated in the study of revising the ASA emergency overloads and set up for other types of transformers (OAP, water-cooled, and so forth), which have characteristics different from those for self-cooled transformers.

When calculating the aging during an overload, it may be assumed that the hot spot is maintained during the entire time period. This is not strictly correct, since the hot spot reaches the maximum value only at the end of the time period. The question naturally arises as to how much greater overload could be carried if the aging was based on the actual increasing hot spot and then increased sufficiently to produce the same aging. The increased overload is not so great as might be expected. For example: if we take the 5*a* class of transformer (Table VII) for two hours following full load, and integrate the actual temperature area by the formula given in reference 3, it is found that a maximum temperature of 134.5 degrees centigrade at the end of two hours results by the eight-degree rule in the same aging as 128 degrees centigrade maintained for two hours. The 6.5 degrees centigrade higher hot spot permits a 4.5 per cent greater load. For the eight-hour overload (115 degrees centigrade hot spot) the difference in the overload is only 3.5 per cent, and, for longer times, the difference becomes less and less. Therefore, if it is assumed that the hot spot exists during the entire time period, the error is small and on the safe side.

A Comparison With Laboratory Aging Data

Having arrived at certain temperature limits and overload values, based partly on past experience and partly on the eight-degree rule, it will be interesting to see what per cent of life these proposed temperature limits will use up for a given time period, based on the laboratory tests. The 120 degrees centigrade aging curves shown in Figure 14 of Mr. Clark's paper appear to be the best ones to use for this purpose. For example: using one per cent water content, these curves give the following approximate hours of life at 120 degrees centigrade:

Per cent of original life	50	0
Hours at 120 degrees centigrade	100	430

Taking the 50 per cent life level we have 100 hours/50 per cent = two hours to use up 1 per cent of initial life at 120 degrees centigrade, or one hour to use up one per cent at 128 degrees centigrade which is within eight degrees of the 136 degrees centigrade suggested for one hour. That is, one hour's operation at 136 degrees centigrade would use up between one and two per cent of the life, depending on whether the 136 degrees centigrade temperature was maintained during the entire time, or reached the maximum value at the end of one hour. In the latter case—as pointed out above—the 136 degrees centigrade would be equivalent to several degrees lower continuous temperature. By this process of reasoning and by the assumptions made, the suggested hot-spot limits would use up from one to two per cent of life per operation. Wide variations from these life values would, of course, be obtained for other than one per cent water content. The estimates of life depend entirely upon the assumptions made.

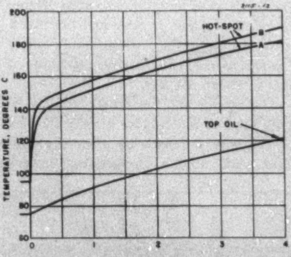


Figure 12. Calculated temperatures for 200 per cent load following full load of transformer having

1. 65 degrees centigrade hot-spot rise
2. 45 degrees centigrade top oil rise
3. 2.7:1 loss ratio
4. Time constant of 5.4

at rated load

5. In 30 degrees centigrade ambient
6. Rises vary as loss$^{0.8}$

Curve *A*—No corrections made for change in resistance and change in viscosity of oil with temperature

Curve *B*—Corrections made for both temperature and oil viscosity

Table C

	Usual Variations	
	Self-Cooled	OAP
Top oil rise over ambient	40 to 50 C	35 to 45 C
Hot spot over top oil	10 to 20 C	15 to 30 C
Loss ratio (copper to iron)	2:1 to 3:1	3.5:1 to 5:1
Time constant	3 to 7	2 to 4.5

At any rate, these laboratory tests have brought out very forcibly a new factor that has not been taken into consideration in the past, namely, water content in the insulation. We seem to make progress in steps. In the early days it was assumed that "temperature" alone was the controlling factor, and later it was recognized that "time" was as important as temperature. Now, we have a third factor, "water content," to take into consideration which appears to be fully as important as "time" and "temperature."

Standardizing Emergency Overloads

Before attempting to standardize emergency overloads, the operators' views are needed on some of the factors discussed above. It is recommended that the

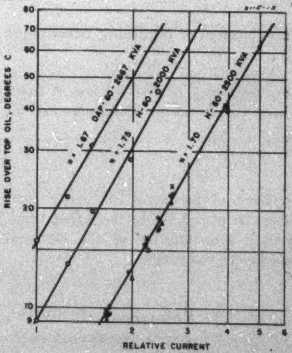


Figure 13. Hot-spot temperature indicator tests

... Normal position
xxx Intermediate position
ΔΔΔ Maximum raised position
Indicator heating coil placed in top oil

problem of preparing emergency overloads be placed before the AIEE transformer subcommittee of the electrical machinery committee.

References

1. Loading Transformers by Temperature, V. M. Montsinger. AIEE Transactions, volume 49, 1930, April section, page 776.

2. Overloading of Power Transformers, V. M. Montsinger, W. M. Dann. AIEE Transactions, volume 53, 1934, October section, page 1353.

3. Effect of Load Factor on Operation of Power Transformers by Temperature, V. M. Montsinger. AIEE Transactions, volume 59, 1940, November section, pages 632–6.

4. Transformer Engineering (book), L. F. Blume, A. Boyajian, G. Camilli, V. M. Montsinger. John Wiley and Sons, Inc., New York, N. Y.

5. Hot-Spot Winding Temperatures in Self-Cooled Oil-Insulated Transformers, F. J. Vogel, P. Narbutovskih. AIEE Transactions, volume 61, 1942, March section, pages 133–6.

6. Discussion by L. Wetherill of reference 5. AIEE Transactions, volume 61, 1942, pages 418–20.

7. Predetermination of Self-Cooled Oil-Immersed Transformer Temperature Before Conditions are Constant, W. H. Cooney. AIEE Transactions, volume 44, 1925, page 611.

8. Cooling of Oil-Immersed Transformer Windings After Shutdown, V. M. Montsinger. AIEE Transactions, volume 36, 1917, pages 711-34.

Induced Voltages on Transmission Lines

C. F. WAGNER
FELLOW AIEE

G. D. McCANN
ASSOCIATE AIEE

ABOUT the year 1929 it first became appreciated that the lightning discharge is not instantaneous. The recognition of this fact had a profound influence upon the theories of lightning protection, the emphasis being diverted from induced to direct strokes[1] as the agent to be guarded against. With the meager information then available the calculations indicated that induced voltages were unimportant when the finite time of discharge was taken into account.[2] The more accurate information available today calls for a critical analysis of this subject, which is the purpose of the present paper.

A brief résumé of our present knowledge of stroke mechanism will be given, which will form the basis for determining simplifying assumptions to be used in calculating the electric field at the ground. The voltage surges produced on transmission lines by these gradients will then be computed. It will be shown that the over-all influence of the variation of stroke characteristics from the simplifying assumptions is not very great and can, in general, be neglected. With these voltage surges established and from other known time-probability characteristics of lightning strokes, a probability curve giving the frequency of occurrence of induced voltages as a function of their crest magnitude will be computed.

Mechanism of Lightning Discharges

Figures 1 and 2 illustrate the essential characteristics of the discharge[3] based upon the work of Schonland and his associates.[4–8] All strokes to ground or to objects as low as transmission towers originate from charge centers in the base of the cloud. At least 90 per cent of the strokes are of negative character. In the course of the heterogeneous formation of the charges the electric gradient finally reaches a magnitude sufficient to initiate a discharge. The original path is blazed by the "pilot streamer" which propagates toward earth at the most frequent velocity of 0.5 foot per microsecond ($^1/_{20}$ of one per cent of the speed of light). Its path is very irregular, and branching occurs frequently in the direction of propagation. The luminosity is low, and the current does not exceed a few amperes. At intervals ranging from 20 to 90 microseconds "stepped leaders" are formed in this same path. These travel at a much higher velocity of about three per cent of light or 30 feet per microsecond. Upon catching up to the pilot streamer these leaders pause. The current seldom exceeds 200 amperes.[7,9] These initial streamers form an antennalike system upon which negative charge is distributed from the original charge center in the cloud.

All available evidence[3] indicates that only very short upward streamers from the earth occur for strokes to open ground or relatively low objects such as transmission lines. They are probably only a few feet in most cases and certainly do not exceed about 100 feet.

The charge on the stroke channel is then lowered much more rapidly the rest of the way to earth by the very brilliant and rapidly propagating "return streamer" which in its visual manifestation appears as a stroke whose head moves upward toward the cloud at a velocity of about 10 per cent of the speed of light or 100 feet per microsecond. The charge on the channel is discharged continuously as the head progresses, which accounts for the high channel and ground currents shown in the lower portion of Figure 2. The ground current rises to a crest ranging from a few hundred amperes to as high as 160,000 amperes[10] in a time of the order of one to ten microseconds and remains at a relatively high value during the period that the return streamer is rapidly discharging the charge in the space between cloud and ground. The wave shape of this current is determined by the rate of discharge of the stroke channel which is a function of the velocity of the return streamer along the channel and the charge distribution.

After the return streamer reaches the cloud, there may occur a more gradual lowering of charge from cloud to ground down the conducting path produced by the return streamer. Charge tapped by streamers into the cloud from the region in which the stroke was initiated may thus produce a current tail of a few hundred or only a few amperes which, however, may persist for 10,000 or 20,000 microseconds.[10]

Multiple strokes consisting of separate discharges down the same stroke channel may be formed by attraction of streamers from another charge center in the cloud. Streamers from this second charge center form a path to the original charge center, and the charge is then lowered by a "dart leader" down the channel blazed by the first discharge. This seldom shows evidence of the stepped character of the first discharge. Its velocity is of the order of one per cent of the speed of light or ten feet per microsecond. Appreciable branching rarely occurs for such subsequent discharges. As the dart leader reaches the earth, its associated charge is further lowered to earth by a return streamer similar in character to that of the first discharge. About 50 per cent of all strokes are multiple, and as many as 40 separate components have been photographed. The time intervals between

---

Paper 42-103, recommended by the AIEE committee on power transmission and distribution for presentation at the AIEE summer convention, Chicago, Ill., June 22–26, 1942. Manuscript submitted April 8, 1942; made available for printing April 28, 1942.

C. F. Wagner and G. D. McCann are both with the central station engineering department, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., Mr. Wagner as manager and Mr. McCann as transmission engineer.

The authors acknowledge the assistance of R. W. Watson and D. E. Morgan who did most of the calculations in connection with this work.

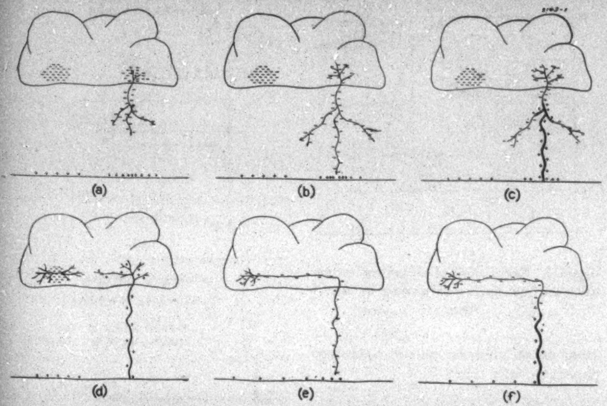


Figure 1. Diagram showing charge distribution at various stages of lightning discharge

(a) Charge centers in cloud
Pilot streamer and stepped leader propagate earthward
Outward branching of streamers to earth
Lowering of charge into space beneath cloud
(b) Process of (a) almost completed
Pilot streamer about to strike earth
(c) Heavy return streamer
Discharge to earth of negatively charged space beneath cloud
(d) First charge center completely discharged
Development of streamers between charge centers within cloud
(e) Discharge between two charge centers
Dart leader propagates to ground along original channel
Dart leader about to strike earth
Negative charge lowered and distributed along stroke channel
(f) Heavy return streamer
Discharge to earth of negatively charged space beneath cloud

components have been found to vary from 0.0005 to 0.5 second with an average of about 0.03 second.

Effect of Distance From Line on Terminating Point of the Stroke

There is a minimum horizontal distance from the line below which all strokes strike the line. As the distance becomes greater, some strokes strike the line and others the ground, and then more and more of them go to ground. Finally all terminate on the ground. The curve of Figure 3 shows this effect as determined from model studies.[11] These studies indicated that for strokes with no appreciable ground streamer the division of strokes is essentially independent of cloud height and a function only of A/h as shown in the figure. The maximum induced voltages occur when the stroke strikes the ground at the minimum distances from the line which are about three times the line height or 75, 150, and 300 feet for line heights of 25, 50, and 100 feet, respectively.

Basic Stroke Mechanism for Induced Voltage Calculations

Although the lightning discharge is quite complex, and the various factors such as the leader velocities, currents, and charges have a wide range of variation, it has been found that the electric field at the ground and the induced voltage on a transmission-line conductor can be determined quite accurately if the stroke discharge is represented by a fairly simple mechanism.

The basic mechanism for the component of a stroke, illustrated in Figure 4, can be divided into three distinct stages.

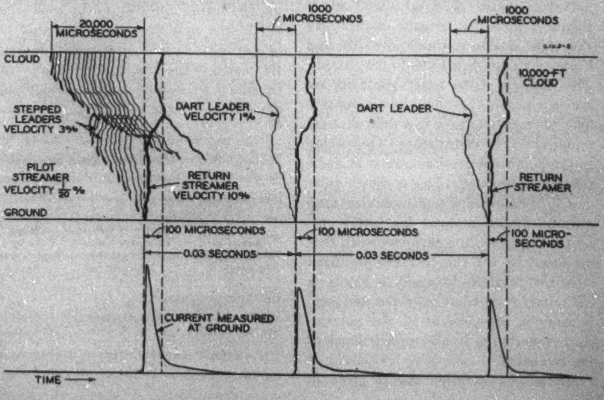
(*a*) *Condition Before the Start of the Discharge.* The charge, $-Q_0$, involved in the initial high-current portion of a single-stroke component (or that lowered onto the stroke channel by the initial leaders) is assumed concentrated in a point source at a height, H, above ground. A height of 2,000 meters or 6,560 feet was chosen, as it was found to be a mean value for cloud heights.

(*b*) *First Stage of Lowering Charge.* At the initiation of the discharge the charge, $-Q_0$, is lowered as a uniform line charge, q coulombs per foot, onto the stroke channel by an initial downward leader moving at the uniform velocity, v, of $^1/_{20}$ of the speed of light or approximately 0.5 foot per microsecond (the pilot streamer velocity). At the end of this period as the streamer reaches earth, the charge in the original charge center is zero. No initial upward streamer from the ground is considered.

(*c*) *Second Stage of Lowering Charge.* At the instant the pilot streamer reaches ground, the return streamer starts propagating upward toward the cloud at the uniform velocity, V, of $^1/_{10}$ that of light or approximately 100 feet per microsecond It instantaneously discharges each section of the stroke channel reached. The current flowing at ground and at each point of the return streamer is of the same magnitude and constant with time during the period that the return streamer is propagating up to the cloud. A current of 100,000 amperes is used resulting, for the given return streamer velocity, in a leader charge, q, of 1.02×10^{-3} coulombs per foot, an initial leader current, i, of 500 amperes, and (for $H=$ 6,560 feet) an initial cloud charge, $-Q_0$, of -6.66 coulombs.

Figure 2. Diagram showing lightning mechanism and ground current

All velocities expressed in per cent of the speed of light which is 984 feet per microsecond or approximately 1,000 feet per microsecond



In all three stages the earth is represented as a perfectly conducting plane by a system of image charges and currents in an image channel.

Ground Gradients

The electric gradient at any point, P, on the ground for the assumption of perfect conducting plane, has only a vertical component that can be resolved into

(*a*). That due to the instantaneous distribution of charges in the stroke channel.

(*b*). That due to the instantaneous current in the stroke.

When either of these conditions change rapidly (with the speed of light as a criterion), it is necessary to take cognizance of the finite time required for the disturbance to propagate to the point in space under consideration.

Expressions for the calculation of the three stages of the discharge enumerated in Figure 4 have been derived in appendix I with the following results.

(*a*) *Before the Start of the Discharge.* Figure 5 shows the initial field as a function of the horizontal distance, d, from the charge center.

(*b*) *First Stage of Lowering Charge.* The left-hand side of Figure 6 shows the ground gradient due to the electric charges alone for various distances, d. Note that the gradient for strokes near the line increases from the value given in Figure 5 at zero time to the extremely high values, attained at the instant the initial streamer strikes the earth. These are plotted in Figure 7. The contribution of the current during this time is negligible, and the processes involved are so slow that the time for transmission of the effect is negligible.

(*c*) *Second Stage of Lowering Charge-Return Streamer.* The right-hand side of Figure 6 gives the ground gradient for this stage due to the charges alone, and Figure 8 the gradients due to the current. The polarity of the latter gradients are opposite to those produced by the charges. This phenomenon is so fast that the time required for the disturbance to propagate to the point under consideration must be taken into account. This is shown quite clearly in Figure 8. For distances less than 10,000 feet the gradient due to the current is negligible in comparison with that produced by the charges. For the fixed streamer velocities assumed in Figure 4, the relative contribution of the two components is independent of the current, since the charge is also proportional to the current, I.

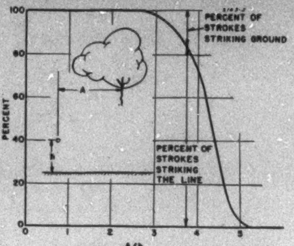


Figure 3. Distribution of strokes for various distances from line as determined by model tests[11]

Special consideration will be given to gradients at large distances in a separate paper at a later time.

Voltage Waves on Conductors

If a long horizontal conductor were divided into short sections, and each section insulated from ground and from the other sections, the voltage appearing at any point at any instant would be merely the voltage gradients at the point times the height of the conductor. Actually, however, transmission-line conductors are continuous. If consideration be given first to an extremely long conductor, then the voltages appearing upon the conductor are free to travel in the form of traveling waves. If the conductor is originally at zero potential, and a voltage wave such as shown by the full line in Figure 9a is suddenly induced, then this wave is broken up into two components each of half the value of the original wave, one half traveling to the right and the other half to the left. After the passage of the waves a negative charge remains on the conductor, representing the negative of the positive charge carried away by the two waves. If a second induced wave be applied instantaneously to the conductor before the first wave will have had an opportunity to have propagated beyond the region of the first, the two waves can be added as shown in Figure 9b, giving the resultant wave indicated. The same

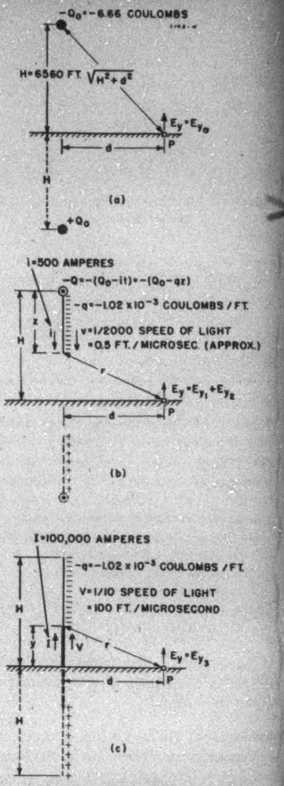

$-Q_0 = -6.66$ COULOMBS
H=6560 FT.
$\sqrt{H^2+d^2}$
$E_y = E_{y_0}$
d
P
H
(a)
$+Q_0$
I=500 AMPERES
$-Q = -(Q_0 - it) = -(Q_0 - qz)$
$-q = -1.02 \times 10^{-3}$ COULOMBS / FT.
v=1/2000 SPEED OF LIGHT =0.5 FT. / MICROSEC. (APPROX.)
z
i
H
r
$E_y = E_{y_1} + E_{y_2}$
d
P
(b)
I=100,000 AMPERES
$-q = -1.02 \times 10^{-3}$ COULOMBS / FT.
V=1/10 SPEED OF LIGHT =100 FT. / MICROSECOND
H
y
I
V
r
$E_y = E_{y_3}$
d
P
H
(c)

Figure 4. Basic stroke mechanism for no ground streamer

process can be carried out indefinitely, as shown for four independent waves in Figure 9c. Thus it is necessary only to divide the continuously increasing inducing voltage wave into finite increments for finite increments of time. Each of the increments is then split up into two halves which are superposed upon the previous halves of increments, each displaced in space along the wire in accord-

Table I. Mean Values of Return-Streamer Velocity and Corresponding Correction Factors to Be Used in Calculating Crest Voltage Induced by a Stroke With a Given Current Magnitude

Range of Stroke Current in Amperes	Range of Return-Streamer Velocity in Feet Per Microsecond	Mean Velocity for Each Current Range	Correction Factor, K, to Be Applied to Voltages Given in Figure 12* Stroke Distance in Feet			
			150	300	600	1,000
0– 50,000	50–190	120	0.93	0.97	1.00	1.00
50,000–100,000	190–330	260	0.70	0.84	0.97	1.00
100,000–160,000	330–480	400	0.61	0.66	0.85	1.00

*For a given crest stroke current (I) in amperes and crest voltage E_m as given by Figure 12, actual voltage is given by the equation $E_m' = \frac{E_m K I}{100{,}000}$.

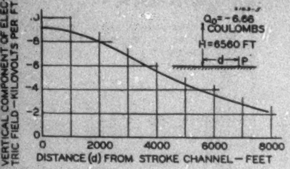


Figure 5. Electric ground gradient just prior to start of discharge

ance with its own increment of time. The increments of field at each time interval as a function of distance, x, along the line can be obtained from curves similar to those of Figure 6 where the distance, d, to be used is the square root of the sum of the squares of the distance along the line and the minimum distance, A, of the stroke from the line. The time intervals which must be chosen to give sufficient accuracy depend upon the rate of change of the field both with time and distance along the line. The greater the rate of change with both of these quantities, the smaller the time intervals have to be. For stroke distances in excess of the minimum at which indirect strokes can occur, as given by Figure 3, the rate of change of field with time is so small for the mechanism of Figure 4 compared to its decrease with distance along the line, in the period the initial streamer is propagating to ground, that the induced voltage is insignificant. The very small increments of voltage, which are negative for negative strokes, and carry away negative charge, move away so rapidly that they do not accumulate an appreciable voltage. If the line is of finite length and open-circuited at both ends with no leakage paths to ground, the successive waves considered in this graphic analysis will produce positive reflections at the line ends which will propagate back to the region under the cloud, return the negative charge, and allow the conductor to rise in potential. However, for actual transmission lines the terminal equipment and other leakage paths will allow charge to flow onto the line and keep it at zero potential during this initial phase of the stroke discharge.

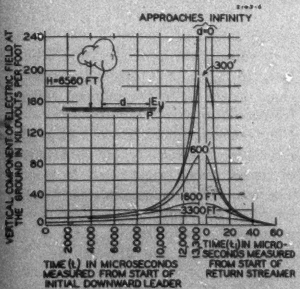


Figure 6. Ground gradient as function of time and distance from stroke channel

Stroke mechanism of Figure 4 (I=100,000 amperes)

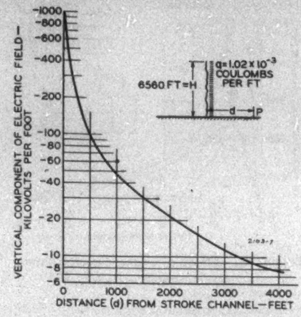


Figure 7. Maximum value of ground gradient that occurs at instant initial streamer reaches earth

Stroke mechanism of Figure 4 (I=100,000 amperes)

During the progress of the return streamer, the rates of field change with time are sufficient to induce a voltage. The negative field is decreasing, so that the rate of change is positive, inducing a positive voltage which propagates off along the line carrying away the positive charge accumulated in the previous period of the discharge. Time intervals of 0.2 microsecond were found small enough for accurately calculating the induced voltage for return streamer velocities of the order of 100 feet per microsecond. Figure 10 shows the voltage induced along an infinitely long conductor 50 feet above the ground at successive intervals of time after the start of the return streamer for a stroke 300 feet from the line. The maximum voltage in all cases is induced at or very near the point on the line closest to the stroke channel. The magnitude of the voltage propagating down the line is, before being attenuated by propagation, only slightly less than the maximum voltage and has the same wave shape. In this case it is 86 per cent of the maximum.

In Figure 11 are shown curves of the maximum voltage waves induced by strokes in conductors at various distances from the stroke. These occur at approximately the point in the line nearest the stroke channel. Since the voltage is proportional to the conductor height, the curves can be applied to heights other than 50 feet. Both the time to crest and time to half value increase with stroke distance. They range from 2 to 20 microseconds and 6 to 36 microseconds, respectively, for distances from 150 to 1,600 feet. In Figure 12 are curves showing the maximum crest voltage induced on lines of various heights as a function of stroke distance. The maximum voltage which can be induced by a stroke with a current of 100,000 amperes and the mechanism of Figure 4 is 970 kv for a 100-foot conductor and 1,070 kv for both 50-foot and 25-foot conductors. The voltage is higher for the latter two, be-

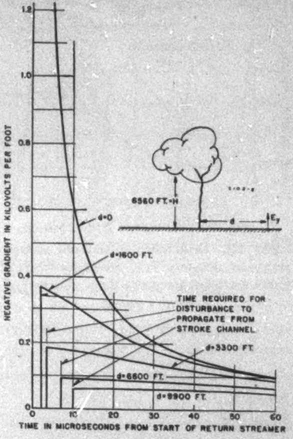


Figure 8. Ground gradient due to current in return streamer

Stroke mechanism of Figure 4 (I=100,000 amperes)

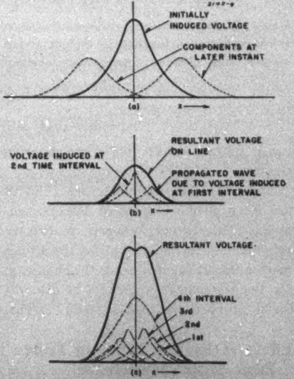

INITIALLY INDUCED VOLTAGE
COMPONENTS AT LATER INSTANT
(a)
x
VOLTAGE INDUCED AT 2nd TIME INTERVAL
RESULTANT VOLTAGE ON LINE
PROPAGATED WAVE DUE TO VOLTAGE INDUCED AT FIRST INTERVAL
(b)
x
RESULTANT VOLTAGE
4th INTERVAL
3rd
2nd
1st
(c)
x

Figure 9. Illustration of step-by-step method of graphically calculating voltage induced on transmission-line conductor

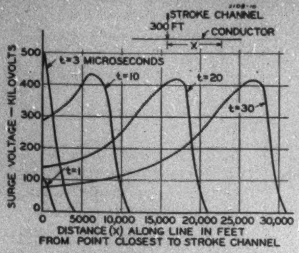


Figure 10. Distribution of induced voltage along an infinitely long conductor 50 feet above ground at successive times after start of return streamer

Stroke mechanism of Figure 4 ($I = 100{,}000$ amperes), and stroke 300 feet from conductor

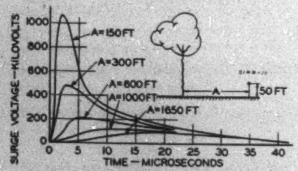


Figure 11. Maximum voltage induced on a conductor 50 feet above ground

Stroke mechanism of Figure 4 ($I = 100{,}000$ amperes)

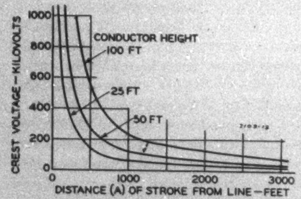


Figure 12. Maximum crest voltage induced on a conductor as a function of height above ground and distance from stroke channel

Stroke mechanism of Figure 4 ($I = 100{,}000$ amperes)

cause indirect strokes can occur closer to the line.

The voltage curves of Figures 11 to 13 are for a stroke current of 100,000 amperes. The voltage, however, is directly proportional to the current for the stroke mechanism of Figure 4 since the charge, q, for fixed streamer velocities is also directly proportional to the stroke current.

Effect of Factors Differing From Basic Stroke Mechanism

It is now possible to determine the accuracy of the assumed basic stroke mechanism and the effect of the variation of the important factors.

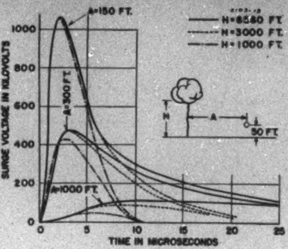


Figure 13. Effect of stroke channel height upon induced voltage

$I = 100{,}000$ amperes

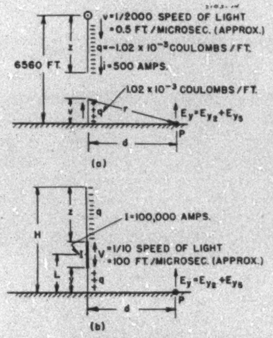


Figure 14. Mechanism assumed for strokes with upward ground streamers

(a). When downward leader reaches distance 2L from ground, upward streamer is initiated with velocity, v, which distributes charge, q, along its channel

(b). After streamers meet, return streamer propagate from meeting point with velocity, V, and annul distributed charge as they proceed

HEIGHT OF STROKE CHANNEL

The only data[11] available pertaining to stroke channel height are that on cloud base heights. Although the cloud source of a stroke may not coincide with the cloud base, the effect of stroke channel height is so small that the difference is not important. For relatively flat terrain the bases of cumulo-nimbus or thunder clouds are usually higher than 1,000 feet with an upper limit of about 30,000 feet and a mean of about 2,000 meters or 6,560 feet. Occasionally, however, strokes to ground occur from cumulus clouds as low as 500 feet, and in mountainous regions the clouds may envelope peaks. The basic calculations have been made for the mean cloud height. Only low channel heights will give a significant variance from the calculations made for a height of 6,560 feet.

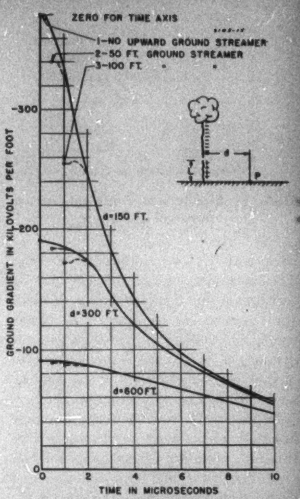


Figure 15. Effect of upward ground streamers on ground gradient during period of return streamer

Mechanism of Figure 14 ($I = 100{,}000$ amperes)

For the same current and the typical streamer velocity of Figure 4 the minimum channel height for which the variance from the mean stroke length can be neglected depends upon the distance from the stroke channel. For d equal to 150 feet a 1,000-foot stroke gives a maximum ground gradient only ten per cent less than for the mean (6,560) height. For d equal to 300 feet, the corresponding height is about 2,000 feet, and for d equal to 1,000 feet, about 4,000 feet. The percentage effect of channel height on the crest value of the induced voltage is even less. In Figure 13 are shown the effect of cloud height upon the induced voltage for I equal to 100,000 amperes and a constant uniform distribution of charge and return streamer velocity, the total charge, of course, being proportional to height. The approximate minimum cloud heights for which only a ten per cent variance is produced in the crest magnitude of the induced voltage are $H = 500$ feet for $d = 150$ feet, $H = 1{,}000$ feet for $d = 300$ feet and $H = 3{,}500$ feet for $d = 1{,}000$ feet. One thousand feet is the maximum distance for which significant voltages will be induced on lines of practical heights. Lower cloud heights do, how-

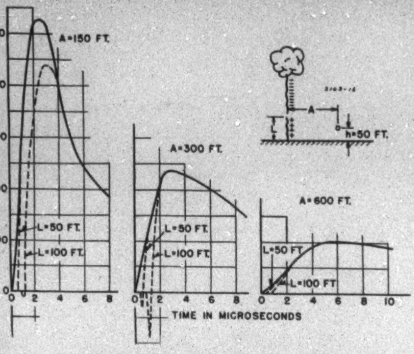


Figure 16. **Effect of upward ground streamers upon induced voltage wave for a 50-foot conductor**

Stroke mechanism of Figure 14 ($I=100{,}000$ amperes)

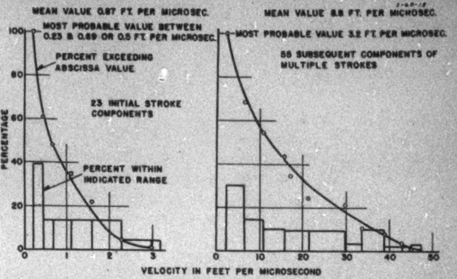


Figure 18. **Percentage distribution curves of the effective downward velocity of initial streamers as determined from Boys camera data of Schonland, Malan, and Collens**[5]

(a, left). Pilot streamer of first component

(b, right). Dart leader of subsequent multiple stroke components

ever, shorten the duration of the tail of the voltage as shown in Figure 13. The number of strokes to relatively flat terrain which will have low enough heights to cause a significant variance from the crest voltages calculated by the formulas of Figure 4 is very small.

CHARGE CENTERS IN CLOUD

As shown by comparison of Figures 5 and 6, the ground gradient due to the charge in a single charge center, whether it be concentrated at a point or distributed over a finite volume, is negligible compared to the total field during the period in which an appreciable voltage is being induced on a conductor. A large amount of charge in other regions of the cloud may produce an appreciable ground gradient but, so long as it is constant, cannot contribute to the voltage wave on the conductor. The probability of two simultaneous discharges occurring sufficiently close to affect the induced voltage wave is extremely small.

UPWARD STREAMERS FROM GROUND

Upward streamers from ground which meet the initial downward streamers are probably seldom over a few feet (and definitely not over 100 feet) in length for strokes to open ground or to relatively low objects. Calculations have been made of the effect of such ground streamers by expressions which can readily be developed from what is already given in appendix I. The stroke mechanism assumed is shown in Figure 14. It is assumed that the initial streamer from the cloud has the same character as the basic case given by Figure 4. When it reaches a height above earth of twice the ultimate height, L, of the ground streamer, a streamer is initiated from the ground and propagates upward at the same uniform velocity, v, depositing positive charge on its channel of the same density, q, as for the downward leader. When the two streamers meet, return streamers are formed at the junction point which propagate in both directions at the uniform velocity, $V=100$ feet per microsecond. In Figure 15 are plotted curves of the electric field during the period of progress of the return streamer for a 50-foot and a 100-foot ground streamer compared with the case of no ground streamer. After the return streamer has reached earth and thus discharged all of the positive charge on the ground streamer, the field is the same as for the case of no ground streamer for corresponding heights of the return streamer channel above ground. The curves of Figure 15 have been plotted with this corresponding portion coinciding for better comparison. Thus, the time of starting of the return streamer (or the zero for the time axis) in Figure 15 must be shifted a different amount for each length of ground streamer to make the curves coincide. The black dots indicate the points of zero time for each case.

The upward ground streamer decreases the maximum ground gradient. However, calculations of the induced voltage, as shown in Figure 16, indicate that even streamers 50 to 100 feet long have little effect upon the crest magnitude of the induced voltage wave. A 100-foot streamer affects the crest only at distances less than 300 feet. For some cases the field tends to increase before decreasing, and this produces a negative peak on the induced voltages. The higher effective velocity of the return streamer during the initial period decreases the front of the wave somewhat. Since most ground

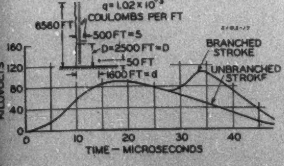


Figure 17. **Successive peaks produced by a branch**

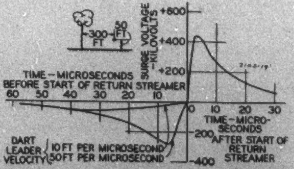


Figure 19. **Comparison between negative voltage induced by dart leaders and voltage induced by return streamer**

Stroke mechanism of Figure 4 ($I=100{,}000$ amperes)

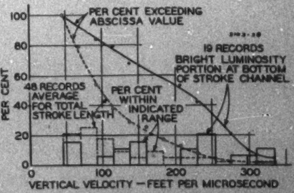


Figure 20. **Percentage distribution curves of the effective upward velocity of the return streamer from Boys camera data of Schonland, Malan, and Collens**[5]

streamers do not exceed a few feet in length their effect can be neglected.

IRREGULARITIES IN THE SHAPE OF THE STROKE PATH

The basic mechanism assumes a straight vertical channel. For strokes whose mean path is about vertical, the tortuousness of the path is unimportant when the effective vertical velocity of the return streamer is used. Placing the same charge in a given vertical section of the channel also gives the same result, regardless of the shape of the path or distribution. Strokes having an appreciable slant cause a greater variance when the channel is not in a perpendicular plane parallel to the line. If the base of the stroke channel toes toward the line, the magnitude of the ground gradient is smaller, and if it toes away, larger than if the channel were vertical. However, the rate of change of the ground gradient with time will be increased in the first case and decreased in the second case, producing a compensating effect on the induced voltage waves. Model tests indicate that those strokes which strike the ground near the transmission line have channels which are not attracted significantly toward the line. In any statistical analysis the only reasonable assumption is a vertical straight channel.

BRANCHING

Schonland, Malan, and Collens[5] have found two characteristic types of branching, those occurring high enough above ground that they do not reach the earth, and those close to ground which contact the ground and form additional channels at the stroke base. This type of branching has been called by them "root branching." The branches leave the main channel at a distance of from 60 to 500 feet above the ground and are from 60 to 150 feet apart at ground. Branches of the first type of appreciable length seldom reach lower than 2,500 feet from ground. Branching usually occurs for the first component of a multiple stroke, but appreciable branches are seldom present in successive components. Of 57 Boys camera[5] records of first components, 54 had branching with an average of five branches each; and of 33 subsequent components, 14 had an average of one very short branch each while the rest had none.

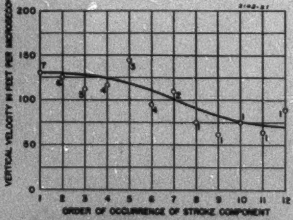


Figure 21. Variation of average return-streamer velocity with order of occurrence of stroke component[4]

Each point is the mean of the indicated number of records

For strokes close to transmission lines, "root branching" will exert some influence on the induced voltage. However, the branches occur practically simultaneously, and for a given configuration of branching an effective location can be determined where practically the same result will be obtained with a single channel. A higher voltage will be induced by an unbranched stroke at the same distance as the nearest branch. If the branches are all the same distance from the line, the difference will be negligible. As shown in the previous section, the character of the stroke within the first 100 feet of ground has little effect on the induced voltage.

Branches 2,500 feet above ground will not appreciably affect the crest magnitude of the ground gradient or the crest magnitude of the induced voltage wave. They may, however, produce additional peaks on the voltage wave. In Figure 17 is shown a calculation made for a horizontal branch 500 feet in length (the average recorded with the Boys camera[5]), 2,500 feet above ground and pointing directly toward the conductor which is 1,600 feet from the main stroke channel. The same uniform charge density is assumed for the branch as for the main channel. It is assumed that when the main return streamer reaches the branch, the branch charge is lowered by a return streamer propagating at the same uniform velocity, $V=100$ feet per microsecond, as for the main channel. This case will produce an abnormally high current peak because of the direction of branching and the high relative charge density on the branch. For the case illustrated in Figure 17, a second peak of 110 kv is produced 33 microseconds after the start of the return streamer. Its magnitude and time of occurrence will be practically the same for shorter stroke distances, so that, to determine the effect of the branch at shorter distances, the increment due to the branching can be superposed on the voltage waves of Figure 11. For d equal to 1,600 feet, the second crest is 22 per cent higher than the first. However, for d less than 1,000 feet, the second crest is less than the first. For most cases of branching, the second crest will be much smaller. Cathode-ray oscillograms of induced voltage waves showing such double peaks that were definitely not due to reflections on the line have been recorded by Perry, Webster, and Baguley.[12] The second crest was in all their records smaller than the first.

VELOCITY OF INITIAL LEADERS

In the basic stroke mechanism it was assumed that the charge was lowered uniformly onto the channel by a single downward moving leader with a constant current. For the first component of a stroke the charge is lowered in steps, some of it by the pilot streamer and some by the stepped leaders. A continual process of recombination of the negative ions with positive ions is probably taking place along the channel. It is this process which very likely causes the building up of sufficient gradient along the channel to start each successive stepped leader. This fluctuation of charge and stepped leader current produces pulses in the ground gradient and has been recorded by Schonland, Hodges, and Collens.[8] It is probably more pronounced in records obtained at large distances but does not alter the mean field. This effect is not present in the dart leaders of successive components, as these leaders are either continuous, or the pulses occur so close together that they can be considered as continuous. Schonland, Hodges, and Collens[8] found evidence that in some cases the pilot streamer of first components slowed up considerably after propagating to within a certain distance above ground. However, the velocity of the leader over the whole stroke path was in all cases too low to induce voltage on a conductor. For most cases the pilot streamer velocity, although having ir-

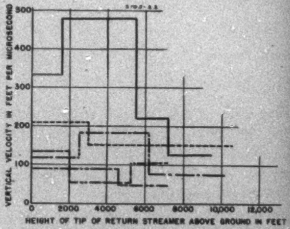
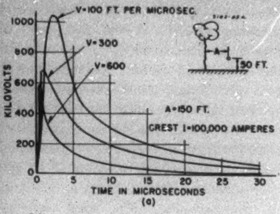
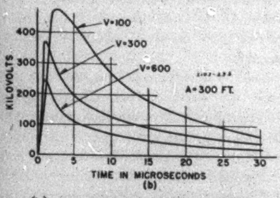


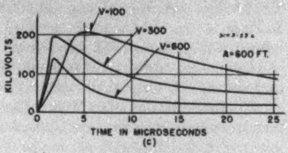
Figure 22. Variation of return-streamer velocity with height above ground for the five strokes recorded by Schonland and Collens[6] for which this analysis could be made



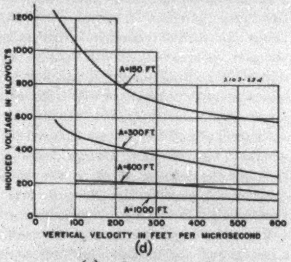
(a). Voltage wave for $A = 150$ feet



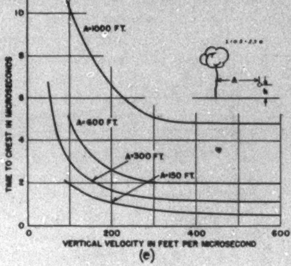
(b). Voltage wave for $A = 300$ feet



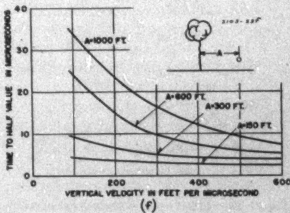
(c). Voltage wave for $A = 600$ feet



(d). Crest magnitude



(e). Time to crest



(f). Time to half value

Figure 23. Effect of velocity, V, of return streamer upon voltage wave induced on conductor

Uniform velocity is assumed and uniform distribution of charge along channel is varied for each case in order to maintain $I = 100{,}000$ amperes

regularities, had the same mean velocity over the entire stroke path.

An initial leader velocity of 0.5 foot per microsecond was used for the basic calculations. In Figure 18 are percentage distribution curves, plotted from the Boys camera records of Schonland, Malan, and Collens,[5] showing the range of variation of the average effective vertical velocity of both the pilot streamer and the dart leader. These values (as were all vertical velocities) were calculated from the two dimensional velocities obtained from the Boys camera photographs by dividing them by 1.3 the average ratio found[5] to exist between this and the straight line path on the film. Only the extreme upper range of the pilot-streamer velocities is high enough to induce appreciable voltage during this period of the discharge. The dart-streamer velocities are, however, high enough to produce a significant voltage. The most common value for this velocity is ten feet per microsecond and the upper limit is 50 feet per microsecond.

In Figure 19 are plotted the voltages induced by leaders of these velocities on a conductor 50 feet above the ground at a distance of 300 feet. For dart-leader velocities of 10 and 50 feet per microsecond the crest magnitude of the negative voltages are 14 and 63 per cent, respectively, of the subsequent positive crest voltage produced during the return-streamer period. For the extreme pilot-leader velocity of three feet per microsecond it will be about four per cent. The duration of the negative induced voltage is about proportional to the velocity of the leaders as shown in Figure 19.

VELOCITY OF RETURN STREAMER

Figures 20 to 22 give data on the range of variance of the velocity of the return streamer as obtained from Boys camera records.[4,5] Here also the one dimensional vertical velocity is considered. In Figure 20 part of the data are for records for which only the average velocity for the total stroke path could be determined, and part for records for which the average velocity of the initial bright luminosity portion could be measured. This is the portion of the stroke path below the first main branch. As shown by Figure 20, the average velocity for the total path varies from 50 to about 330 feet per microsecond with a most common value of about 80 feet per microsecond. For the bright luminosity portion the range is the same with a most common value of about 240 feet per microsecond. The data[5] from which Figure 20 was plotted did not permit segregating the stroke components in their order of occurrence. This could be done, however, for some of the records as published earlier,[4] and in Figure 21 is shown the variation of average return-streamer velocity with the order of occurrence of the stroke component of a multiple stroke. The velocity of successive strokes is generally less than the previous one with the initial component having the highest value. It has also been found that the stroke luminosity generally decreases with successive components in the same manner, and that the higher velocities are associated with the higher luminosities. Stroke-current measurements show the same general trend, so that the evidence is quite strong that higher return-streamer velocities are associated with higher stroke currents. Also, the higher return-streamer velocities of first components are associated with considerable branching. It is probable that the increased ground gradient produced by the branches accounts for the higher velocities and stroke currents of first components.

In Figure 22 are plotted the velocities of five Boys camera records published by Schonland and his associates[5] for which the distribution of velocity along the channel could be determined. It is questionable that the base of the stroke channel below about 100 feet was recorded. However, the crest magnitude and wave shape of the tail of induced voltages are not affected by the character of the stroke mechanism at the base of the channel. This was well illustrated by the effect of ground streamers, which would have the maximum effect of any condition which would occur at the base. The lowest portion of the stroke channel which need be considered in determining the crest magnitude and the

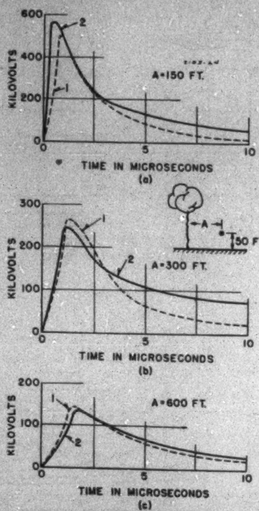


Figure 24. Effect upon voltage wave of return-streamer velocity varying along stroke channel

Curve 1—Uniform charge distribution with streamer velocity given by solid curve of Figure 22

Curve 2—Uniform charge distribution and uniform $V = 480$ feet per microsecond

Crest current = 100,000 amperes for both cases

tail of the voltage wave depends upon the stroke distance from the line. For 150 feet it is about 75 feet above ground, for a distance of 300 feet it is about 150 feet, and for a stroke distance of 600 feet, about 400 feet. In general, the abrupt decrease in velocity of the return streamers in Figure 22 is associated with the streamer reaching a branch point. It probably also varies somewhat between branches but not enough to be recorded. The maximum known effective vertical velocity is the 480 feet per microsecond of the stroke represented by the solid curve of Figure 22. The maximum velocity along the channel also occurred for this stroke and is about 600 feet per microsecond.

Calculations have been made of the effect of return-streamer velocity on the induced voltage wave. In Figure 23 is shown the effect of velocity, with the charge density so adjusted as to maintain the same stroke current. Increasing velocities decrease the crest magnitude, time to crest and duration of the voltage. As shown in Figure 23d, the effect on the crest voltage is most pronounced at the short-stroke distances and lower velocities. For a stroke distance of 150 feet the upper limit of vertical velocity of $V = 480$ feet per microsecond causes a decrease in the crest voltage, as calculated for $V = 100$ feet per microsecond, of 44 per cent. At 300 feet it is 40 per cent and at 600 feet, 19 per cent. For greater distances the effect is unimportant. However, the wave shape is affected more at large distances as shown in Figures 26 and 27.

Calculations have also been made to determine the effect of velocity variation along the stroke channel. Figure 24 shows a comparison of the voltage induced by a stroke having a uniform return-streamer velocity of 480 feet per microsecond and a uniform distribution of charge, q, and the voltage induced by a stroke with the same charge distribution but the velocity given by the solid curve of Figure 22. The effect of variation is small, producing a change in crest voltage of 14 per cent at $d = 150$ feet, 6 per cent at $d = 300$ feet and 8 per cent at $d = 600$ feet. The variation has no effect on the front of the wave and little effect on the time-to-half value, although the extreme tail of the wave is decreased by the decreasing velocity.

The variation of return-streamer velocity along the stroke channel is not important, because it does not vary greatly until it reaches the first branch point which is usually at least 2,500 feet above ground. As pointed out earlier, the character of the stroke mechanism above this height has no important effect on the induced voltage. It is then the velocity of the bright luminosity portion or that below the first branch which should be used for voltage calculations.

Variation of Charge Distribution Along Channel

The two principal factors governing the stroke current flowing at ground are the charge distribution, q, and the return streamer velocity, V. The variations of streamer velocity of the five cases of Figure 22 are sufficient to account for the decrease of current recorded for the tails of lightning-stroke currents. Independent data regarding the time for the current at the ground to decrease to half value are given in Figure 25. The normal variance of this time is from 15 to 100 microseconds, with an average of about 50 microseconds. The solid velocity curve of Figure 22 would, for a uniform charge distribution, give a time-to-half value of 16 microseconds; the other curves give times of about 40 microseconds.

Calculations have been made to determine the effect of nonuniform charge distribution on the induced voltage wave. In Figure 26 are compared the voltages induced by a mechanism with a uniform charge and velocity distribution and by the same uniform velocity distribution but ~~a~~ charge distribution such that the current decays exponentially to half value in 40 microseconds. The effect on the induced voltage wave shape and crest magnitude is slight, as would be expected since the charge cannot decrease greatly in the first 1,000 or 2,000 feet and still give the proper current wave shape.

Rate of Discharge of Stroke Channel by Return Streamer

In the fundamental stroke mechanism of Figure 4, it is assumed that, as the return streamer reaches a given height above ground, it immediately discharges all the charge on the channel at that point. It has been suggested by Bruce and Golde[14] that this is not the case. They believe that the charge, after being lowered onto a given section of the channel by the initial leaders, spreads out radially and thus occupies a larger cross section than is immediately made con-

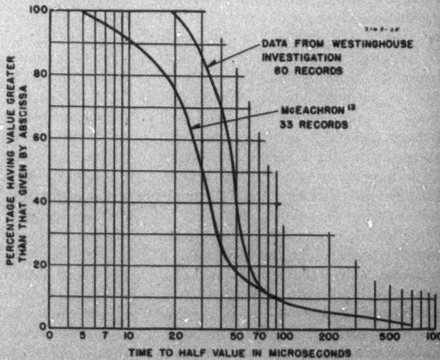


Figure 25. Percentage distribution curves of time-to-half value of lightning-stroke currents

ducting by the return streamer. They further assert "that the darts associated with the leaders of first strokes result from lateral conduction from the initial channel outwards as the tip progresses and the potential at each point behind it rises," and that: "During the return stroke this ionization in each horizontal layer cannot be recombined instantaneously, since the lateral progression of this process will again take time."

Schonland[7] found that the duration of high luminosity at any point after it is reached by either the initial bright portion of the stepped leader (which Bruce and Golde refer to as the "dart leader"), or by the return streamer is of the order of five to ten microseconds. After this the luminosity suddenly diminishes. This is the period in which Bruce and Golde assume the channel to be widening out. Schonland[7] himself has pointed out that luminosity of this duration cannot be due to the excitation period of nitrogen (the principal element involved in the ionization process). Its period is only about 0.1 microsecond.

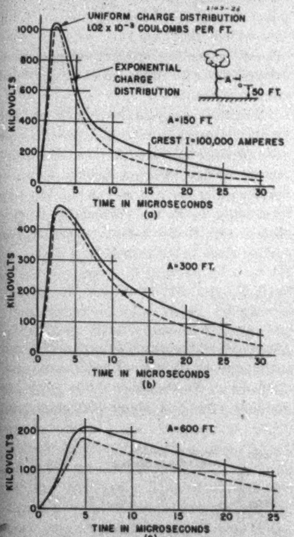


Figure 26. Effect upon voltage wave of variation of charge density along stroke channel

Solid curve—Uniform charge distribution of 1.02×10^{-3} coulombs per foot

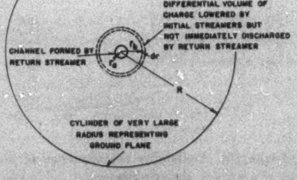
Dotted curve—Exponential charge distribution with maximum charge density of 1.02×10^{-3} coulombs per foot at the ground and decreasing to half value 4,000 feet from ground to give current wave at ground with 40 microsecond tail

For both curves $V=100$ feet per microsecond and crest current = 100,000 amperes

Such lateral ionization may take place. However, even if the return streamer does (as shown in Figure 27) first blaze a conducting channel of smaller cross section than that occupied by the initial charge on the channel, positive charge must immediately flow onto the outer surface of the initial highly conducting channel to maintain it at the low potential it will have by virtue of being in contact with the earth. For the ground gradient calculations, all of the return-streamer portion of the stroke channel can be considered as being at earth potential as the arc drop is small. As shown in appendix II, the amount of positive charge on a given channel section required to maintain the core of the return streamer at earth potential in the presence of estimated surrounding charges of the downward leader is at least 70 per cent of the negative charge left in the space not yet made conducting by the return streamer. Most of the charge is neutralized rapidly by recombination with charges rising in the ground leader; and even if a remaining charge exists, as Bruce and Golde purport, that is not immediately neutralized by this process, additional positive charge to the extent of at least 70 per cent is drawn into the section to maintain the arc core at earth potential. Thus, positive charge flows up the stroke channel at a rate determined by the return-streamer velocity and charge density, q, on the stroke channel, and the effect is essentially the same as though the combination of the two sets of charges were consummated instantaneously.

Positive Charge on Return-Streamer Channel

The analysis of appendix II considers only the relationship the positive charge on the channel must bear to the negative charge in a horizontal section of the conductor. Symmetry about a vertical axis is assumed, and the end effects at the tip of the return streamer are neglected. Because of electrostatic induction with the negatively charged leader that has not yet been discharged, the upwardly extending channel and ground which are all at the same potential, do not have equal charge distribution. Thus, there must be a net distribution of positive charge on the upward streamer. If this charge were uniform, it would produce no effect on the induced voltage, as calculated by the mechanism of Figure 4 for a fixed stroke current. For the same current, the negative charge on the stroke channel would have to be decreased by an amount proportional to the assumed positive charge, and the net field change with time would be the same. The density of this positive charge on the return streamer should be greatest near the tip and should decrease down the channel. Thus, as the streamer is forming upward, the current at ground feeding the streamer will decrease with time. Since the variation of return-streamer velocity alone is sufficient to account for the measured current wave shapes, the variation of charge along the stroke channel cannot be very great, especially for the first 1,000 or 2,000 feet above ground. It probably does not produce so great an effect as the variation of negative charge for the case illustrated in Figure 26.



DIFFERENTIAL VOLUME OF CHARGE LOWERED BY INITIAL STREAMERS BUT NOT IMMEDIATELY DISCHARGED BY RETURN STREAMER
CHANNEL FORMED BY RETURN STREAMER
r_b
r_a
dr
R
CYLINDER OF VERY LARGE RADIUS REPRESENTING GROUND PLANE

Figure 27. Diagram showing cross section of stroke channel

Relation Between Maximum Induced Voltage and Voltage Propagating Along Line

In the previous discussion only the voltage induced on the line at the point nearest the stroke channel, or the maximum voltage, has been considered. For the range of variation of the several factors controlling the induced voltage, the ratio between the voltage which propagates away from this point and the maxi-

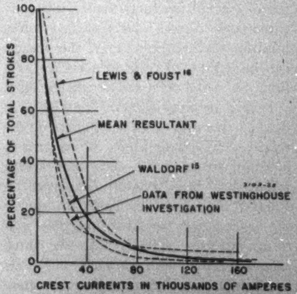


Figure 28. Percentage distribution curves of crest currents at ground in lightning strokes as obtained by various investigators

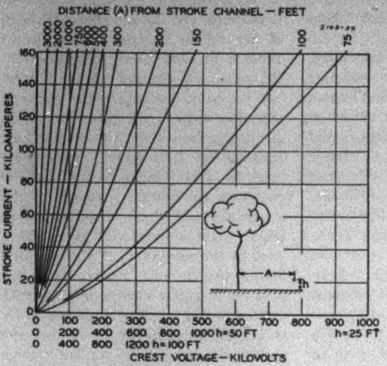


Figure 29. Crest of induced voltage wave as a function of stroke current, distance from channel, and height of conductor

mum (as illustrated in Figure 10) is never less than about 0.8 and is usually greater than 0.9. Thus, for practical purposes it is considered best to use the maximum voltages for both cases.

Summary

The foregoing analysis shows that induced voltages on transmission lines can be calculated to a good degree of accuracy by the stroke mechanism of Figure 4 where the distribution of charge and the return-streamer velocity is assumed constant along the channel. These are the primary factors determining the voltage wave shape and magnitude, and they in turn determine the stroke current. Wave shape is little affected by anything but the return-streamer velocity as summarized in Figures 23e and 23f. As shown in Figure 23d, the effect of velocity upon the crest voltage may be considerable, but it can be taken into account to a good degree of accuracy. The velocity increases with the magnitude of the stroke current. The exact manner in which it does is not known. However, if the measured range of the velocity and the stroke current are each divided into only three equal ranges, and a mean velocity used for each current range, the voltage can be calculated to good accuracy as a function of the crest stroke current only.

Figure 28 gives what is considered to be the best mean probability curve on the relative crest magnitudes of currents in lightning strokes. The maximum current which has been reliably measured is 160,000 amperes.[10] In Table I is shown the segregation of stroke current and return-streamer velocity into the three ranges together with the mean velocities for each range. Correction factors, determined from Figure 23d, applicable to the mean velocities for different distances, are also given in Table I. In Figure 12 are given crest voltage curves based on a stroke current of 100,000 amperes and a streamer velocity of 100 feet per microsecond. The curves of Figure 12 can be used for calculating the induced voltage as a function of crest current, together with the equation and the mean correction factors of Table I. In this manner the curves of Figure 29 were obtained which give the induced voltage as a function of stroke current for various conductor heights and stroke distances.

Effect of Other Conductors and Line Terminal Conditions

As has been shown previously,[27] the voltage induced on an ungrounded conductor is not affected by the presence of other ungrounded conductors. Ground wires, which are perfectly grounded so that their potential remains at zero at all points, reduce the voltage induced on a conductor by the factor $(1-K)$, where K is the coupling factor between the conductor and the ground wires.

However, for long spans and strokes of high current magnitude occurring close to the line, appreciable voltage appears for a short time on the ground wire. Consider for example, a line with 1,000-foot spans and a ground wire 100 feet above ground. As shown by Figures 29 and 23e, a voltage of 975 kv with a 1.2-microsecond front can be induced at mid-span. The rate of rise is too rapid for the reflection from the towers to limit the crest appreciably. Approximately the same potential is induced on both conductor and ground wire. Until the waves reach a tower, there is little potential difference across them. Upon reaching a tower, the voltage wave on the ground wire is reduced sharply, and thus the voltage across the string of insulators is practically the same as though there were no ground wire present. For shorter spans, reflections on the ground wire induce voltages on the conductor that reduce the crest voltage, the limiting value to which they can be reduced being $(1-K)$ times the original value as mentioned above for the ground wire grounded at many points.

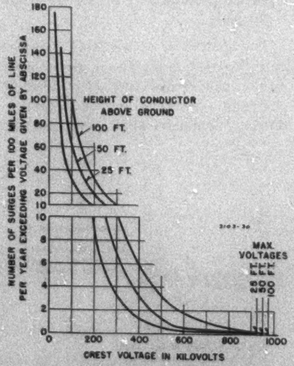


Figure 30. Probability of occurrence of induced voltages

Isokeraunic level = 30

It is interesting to note that the voltage induced on the ground wire for this case can produce appreciable positive current in the towers. For an incident surge, e, propagating to a tower whose footing resistance is R, the current, i_t, flowing down the tower before reflection can arrive from other towers is

$$i_t = \frac{2}{Z+R} e$$

where Z is the surge impedance of the ground wire, or the effective surge impedance if there are several. For one ground wire Z is about 500 ohms; for

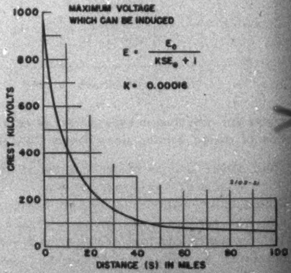


Figure 31. Foust and Menger[30] formula for determination of attenuation of crest magnitudes of voltage waves

two, about 350 ohms. The maximum current would occur if $R = 0$. Then for the case being considered i_t would be 3,900 amperes for one ground wire and 5,600 amperes for two ground wires, respectively. This represents the upper limit of the current which can be induced in the tower of a high-voltage line.

For lines of finite length or containing points of discontinuity such as connected apparatus at various points along the line, the voltage will, of course, be modified by reflections. These can be taken into account by conventional traveling wave theory in which the initial incident wave is that considered here.

Probability of Induced Surges on Transmission Lines

NUMBER OF INDUCED SURGES ORIGINATING ON EACH MILE OF LINE

With a knowledge of the stroke density for a given region or the number of strokes originating from each square mile of sky area, it is now possible to determine the number of surges of a given crest magnitude which will be induced on a line. An average value for the stroke density in a region of flat terrain where the isokeraunic level averages about 30 storm days per year has been found to be ten strokes per square mile.[18] This value and the curve of Figure 3 determine the number of strokes which strike ground within finite bands a given distance from each side of a line of a given height. Figure 28 determines how many of such strokes have a current magnitude of a given value, and Figure 29 how much voltage each induces. By dividing the ground (from the minimum distance from the line for which strokes will strike earth to the maximum distance for which significant voltages will be induced) into such finite bands parallel to the line and adding up the number of strokes in each band which will produce voltages of a given value, probability curves such as given in Figure 30 are obtained. The curves of this figure give the number of surges exceeding each magnitude which is induced at the point nearest the stroke channel per 100 miles of transmission line.

The curves of Figure 30 indicate, as has been pointed out previously,[19] that the number of surges of a significant magnitude which are induced on a transmission line is of the same order of magnitude as the number of strokes which strike it directly. Waldorf[18] has found that on the average 100 direct strokes per mile per year can be expected to lines in the voltage class between 66 and 220 kv with line heights of from 60 to 110 feet and in regions of the same isokeraunic level as given for Figure 30.

The curves of Figure 30 can be used for determining the number of times flashover may occur on a line at points remote from the ends where reflections do not modify the surge voltage. As can be seen from Figure 23f, for distances at which voltages sufficiently great to produce outages (above about 300 kv) are developed, the time to half value varies between 5 and 20 microseconds. The critical flashover voltages for waves of this character are from 10 to 30 per cent higher than for a standard $1^1/_2 \times 40$ wave. Thus, for a 50-foot line with a critical standard impulse value of 300 kv, it is equivalent to 330 to 390 kv on the shorter waves. From Figure 30 this will produce about five outages per 100 miles per year due to induced voltages.

The data of Figure 30 must be distinguished from the total number of surges exceeding each magnitude which will appear at each point on the line, due not only to those originating at the point, but also to those propagating from other points. If it were not for attenuation and reflections, this would be equal to the number per mile times the length of the line in miles. Attenuation is a function not only of voltage magnitude but also of wave shape. However, an estimate of the probability of surges of various magnitudes appearing at any one point on a line can be made with the use of the empirical decrement curve of Figure 31. This curve[20] represents the mean of records obtained on the attenuation of lightning surges. The length of the line and terminal conditions, of course, affect the magnitude of the surges, so that they vary for different line conditions. Each particular line can be studied by traveling wave analysis. Use Figure 30 to determine the number of surges of each magnitude originating from each section of line aud Figure 31 to determine its magnitude after propagation. However, two conditions are sufficiently representative that they can be applied to most cases.

NUMBER OF SURGES AT POINT REMOTE FROM ENDS OF LINE

The first of these is the case for which the line is sufficiently long in both directions from the point under consideration that it can be considered as infinite. Voltage probability curves calculated for this case are given in Figure 32. These were determined by dividing the line on each side of the point being considered into one-mile sections, after deciding from Figure 30 the number of surges of each magnitude which will originate from each section, and from Figure 31 their magnitude by the time they reach the point. A mean distance is assumed for each section. The accuracy of these curves for lines of finite length depends upon the voltage being considered. Figure 31 indicates that no induced surges in excess of 100 kv propagate more than 40 miles; for 200 kv, 25 miles, and for 400 kv, only 10 miles. Thus, the length of line for which these curves may be used varies in an inverse manner with the voltage. For voltages less than 100 kv, the attenuation is so small that for estimating purposes the number of surges of such magnitudes can be considered as proportional to line length and determined directly from Figure 30. These curves are based upon the premise that none of the surges reaching the point under consideration produced flashover before reaching the point.

NUMBER OF SURGES AT OPEN END OF LONG LINE

If a line is open-circuited at its end, the magnitude of the crest voltage at the end is twice that of the incident surge. This is approximately true for lines with transformers connected to the end. Also, since the line extends in only one direction from the point under consideration, only half as many surges can originate from a given distance from the end as for the previous case. The probability curves of Figure 33 were, therefore, obtained from those of Figure 32 simply by picking a particular voltage, doubling it, and assigning to it one-half the number per year as read from the curves.

COMPARISON WITH FIELD DATA

There are very little field data on lightning-surge voltages for which the induced surges can be segregated from those produced by direct strokes. From the data submitted by Cox, McAuley, and Huggins,[21] the authors have chosen the Klydonograph records obtained at the ends of four long lines of voltage class of 110–140 kv without ground wires. Only those records that did not produce flashover were included. The results are plotted by the dotted line of Figure 33. Undoubtedly some of the high-voltage records were due to direct strokes, and at low voltages some surges were not recorded because of the lower limit of sensitivity of the instrument.

Conclusion

The velocity of the initial downward leader of lightning strokes is too slow to produce significant voltages on transmission lines by induction. Only the

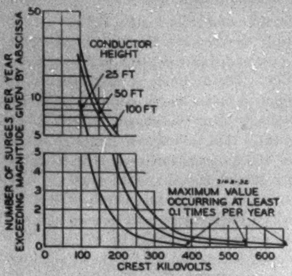


Figure 32. Probability of occurrence of induced voltage at a point remote from the ends of a line

Maximum voltage which can appear:

$h=$ 25 feet—925 kv
$h=$ 50 feet—950 kv
$h=$100 feet—975 kv

return streamers are of importance. The voltages produced by the latter can be determined to sufficient accuracy by representing the return streamer as a vertically propagating conductor whose tip moves with uniform velocity, neutralizing, as it progresses, the uniformly distributed charges on the initial streamer. In spite of the high currents and the short duration of the return streamer, only the charge and not the high rate of change of the current is important. The voltages so produced are proportional both to the height of the transmission-line conductor and the crest of the current in the stroke at ground. It has been found possible to represent strokes by the characteristics enumerated in Figure 4. Of all the factors considered, only variations in velocity of the return streamer affect induced voltages significantly. Return-streamer currents vary with the magnitude of stroke current. By choosing the probability of occurrence of stroke currents and corresponding return-streamer velocities, the probability of occurrence of induced voltages as a function of their magnitude can be determined. Although the voltages induced by lightning on transmission lines may be quite high, those of sufficient magnitude to produce flashover occur so infrequently as to be of little practical importance.

Appendix I. Equations for Electric Field Produced at Ground by Lightning Strokes

During the period in which the initial leaders are propagating toward earth, the change of electrical conditions with time are slow enough that neither the velocity of propagation of the disturbance from the stroke channel to the point at the ground under consideration nor the magnetic effect on the electric field need be considered. However, for the period in which the intense return streamer is propagating up to the cloud, the rapid rate of propagation and high current in the return streamer require the use of electrodynamic equations.

The general solutions to Maxwell's dynamic field equations are given in the practical system of units by the following equations:

$$\mathbf{A}_{P,\,t}=0.1\int\frac{[\mathbf{i}]_{t}-\frac{r}{c}}{r}dv \tag{1}$$

$$V_{P,\,t}=9\times10^{11}\int_{v}\frac{[q]_{t}-\frac{r}{c}}{r}dv \tag{2}$$

$$\mathbf{E}_{P,\,t}=-\nabla V-10^{-8}\frac{\partial \mathbf{A}}{\partial t} \tag{3}$$

where

dv is a differential element of volume
r is the distance from P to the differential volume (dv)
c is velocity of light (3×10^{10} centimeters per second, or 984×10^{6} feet per second)
t is time in seconds
q is charge density
$\mathbf{A}_{P,\,t}$ is retarded magnetic vector potential
$V_{P,\,t}$ is retarded scalar electric potential in practical volts
$\mathbf{E}_{P,\,t}$ is electric field in practical volts per centimeter

The quantities $\mathbf{A}$, V, and $\mathbf{E}$ are given at the point P as a function of time when the integration is carried out over all space occupied by charge except at $r=o$. The retarded values of $\mathbf{i}$ and q, $[\mathbf{i}]_{t-r/c}$ and $[q]_{t-r/c}$ are their values at the time $(t-r/c)$ at which they produce the field disturbance at P at the time t. The ratio r/c is the time required for the disturbance to propagate the distance r at the speed of light.

1. Electric Field Before Start of Discharge

It is assumed that, as shown in Figure 4a, the charge in any one charge center is confined to a small volume which can be represented by the point charge, $-Q_o$, and that the earth's surface is a perfectly conducting plane as represented by the image charge, $+Q_o$. For this static case the field at the point P has only the vertical component given by the equation where dimensions are expressed in feet.

$$E_{yo}=\frac{5.9\times10^{10}HQ_o}{(H^2+d^2)^{3/2}}\text{ volts per foot} \tag{4}$$

2. Electric Field Produced by Stroke With No Initial Upward Streamer From Ground

During Progress of Downward Leader

As shown in Figure 4b, it is assumed that the charge, $-Q_o$, is lowered onto the stroke channel by the initial leader at a uniform rate given by the leader velocity, v, and the uniform charge density, $-q$, per unit length of the channel. Thus, if z is the length of the initial streamer channel at a time, t, as measured from the start of the leader

$$z=vt \tag{5}$$

The current flowing in the channel is

$$i=qv$$

The charge left in the charge center is

$$-Q=-(Q_o-it)=(Q_o-qz)$$

The effect of the ground plane for all of the following cases is represented by an identical motion of the image charge along the image channel. This is accounted for by merely multiplying the field due to the real cloud charges or currents by two. The initial streamer current and the initial streamer velocity relative to the speed of light are so small that the magnetic contribution to the electric field and the retarded potential effect need not be considered.

The field due to the charge left in the cloud is

$$E_{y1}=\frac{5.9\times10^{10}(Q-it)}{(H^2+d^2)^{3/2}}\text{ volts per foot} \tag{6}$$

The field due to the charge on the leader is, when dimensions are in feet,

$$E_{y2}=5.9\times10^{10}\int_{0}^{z}\frac{q(H-z)}{r^3}dz \tag{7}$$

If the charge distribution, q, is uniform

$$E_{y2}=5.9\times10^{10}q\left[\frac{1}{(z^2-2Hz+H^2+d^2)^{1/2}}-\frac{1}{(H^2+d^2)^{1/2}}\right]\text{ volts per foot} \tag{8}$$

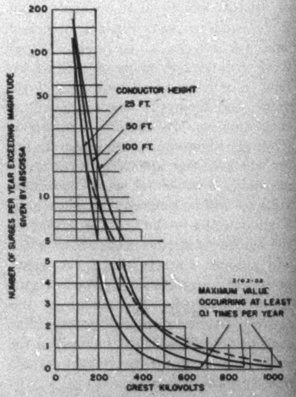


Figure 33. Probability of occurrence of induced voltage at the end of an open-circuit line

Solid curves are calculated
Dotted curve is result of Klydongraph measurements[21]

Maximum voltage which can appear:

$h=$ 25 feet—1,850 kv
$h=$ 50 feet—1,900 kv
$h=$100 feet—1,950 kv

E_{y3} as a function of time is obtained by the substitution of equation 5. The total field during progress of the downward leader is

$$E_y = E_{y1} + E_{y2} \qquad (9)$$

At the instant the downward leader reaches the earth Q is zero and

$$E_y = E_{y2} = 5.9 \times 10^{10} q \left[\frac{1}{A} - \frac{1}{(H^2+d^2)^{1/2}} \right] \text{ volts per foot} \qquad (10)$$

DURING PROGRESS OF RETURN STREAMER

At the instant that the initial leader reaches earth, the return streamer shown in Figure 4c starts upward at the velocity, V, instantaneously discharging each portion of the channel upon reaching it. The velocity is high enough (0.1 to 0.6 that of light) so that the retarded potential effect must be considered. However, since the return streamer just annuls the initial streamer charge, the only point where q is changing at any instant is at the return-streamer tip whose location is designated by y. Thus, for a given height, y, of the return streamer, the field due to electric charge can be expressed by equation 8 upon the substitution $z = H - y$.

$$E_{y3} = 5.9 \times 10^{10} q \left[\frac{1}{(y^2+d^2)^{1/2}} - \frac{1}{(H^2+d^2)^{1/2}} \right] \text{ volts per foot} \qquad (11)$$

The time t_1 (as measured from the start of the return streamer) at which the gradient at P is given by equation 11 for a certain value of y is

$$t_1 = t_2 + \frac{1}{c} \sqrt{d^2+y^2} \text{ seconds} \qquad (12)$$

where t_2 is the time required for the return streamer to reach the height y. For a uniform velocity V, $t_2 = \frac{y}{V}$

$$t_1 = \frac{y}{V} + \frac{1}{c} \sqrt{d^2+y^2} \text{ seconds} \qquad (13)$$

ELECTRIC FIELD CAUSED BY CURRENT IN RETURN STREAMER

As given by equation 1 and Figure 4c, the vector potential at P caused by the uniform distribution of current, $+I$, flowing upward in the return streamer channel and its image, $-I$, is

$$A_y = 0.2I \int_0^y \frac{dy}{r_1}$$

$$A_y = 0.2I \log_e \frac{y + \sqrt{y^2+d^2}}{d} \qquad (14)$$

A_y is independent of the units of length used and the only component of A which contributes to E_y. Furthermore, its other components are zero. From equation 3

$$E_{y4} = -10^{-8} \frac{\partial A_y}{\partial t_1} \text{ volts per centimeter}$$

$$E_{y4} = -30.4 \times 10^{-8} \frac{\partial A_y}{\partial t_1} \text{ volts per foot} \qquad (15)$$

$$\frac{\partial A_y}{\partial t_1} = \frac{\partial A_y}{\partial y} \frac{dy}{dt_1}$$

Thus

$$E_{y4} = \frac{6.08 \times 10^{-8} I [1 + y(y^2+d^2)^{-1/2}]}{y + \sqrt{y^2+d^2}} \frac{dy}{dt_1} \text{ volts per foot} \qquad (16)$$

From equations 12 and 13 for uniform velocity, V,

$$cy = cVt_1 - V\sqrt{y^2+d^2}$$

and for the particular case in which $V = 0.1c$

$$y = 0.10101ct_1 - \sqrt{0.000102c^2t_1^2 + 0.0101d^2} \text{ feet}$$

$$\frac{dy}{dt_1} = 0.10101c - \frac{0.000102c^2t_1}{\sqrt{0.000102c^2t_1^2 + 0.0101d^2}} \text{ feet per microsecond} \qquad (17)$$

E_{y4} as a function of time can thus be determined from equations 16 and 17.

Appendix II

The negative charge lowered by the initial downward leader may be distributed over a section that is considerably larger than the core of the upward streamer. Since the upward streamer is essentially at ground potential, to maintain this potential a positive charge must move from earth to neutralize the effect of the negative charge. The magnitude of the positive charge can be determined as follows: Assume the negative charge lowered in the streamer to have circular symmetry of density $-q_1$ per square centimeter per centimeter length and that the radius of the arc core of the return streamer is r_a. Without invalidating the general conclusions, the earth may be represented by a cylinder of large radius, R, symmetrical about the streamers. End effects will be neglected. Both the earth cylinder and the return streamer are at zero potential.

The charge on an elemental ring of radius, r_1, and thickness dr_1 is $(-2r_1q_1dr_1)$ or dQ_1. The field due to this charge external to r_1 is

$$E_r = \frac{2dQ_1}{r} \qquad (18)$$

The potential of this ring above the outer cylinder is

$$V_1 = \int_R^{r_1} \frac{2dQ_1}{r} dr$$

$$= 2dQ_1 \log \frac{r_1}{R} \qquad (19)$$

The potential due to the charge in the elemental ring for all points within r_1, including the upward leader, is also equal to V_1.

Let dq_u be the charge in the return streamer required to neutralize the negative charge on the elemental ring. The potential of the return streamer above the outer ground cylinder is

$$V_u = \int_R^{r_a} \frac{2dq_u}{r} dr = 2dq_u \log \frac{r_a}{R} \qquad (20)$$

since the potential of the return streamer is zero.

$$V_1 + V_u = 0 = 2dQ_1 \log \frac{r_1}{R} + 2dq_u \log \frac{r_a}{R} \qquad (21)$$

and

$$dq_u = dQ_1 \frac{\log \frac{R}{r_1}}{\log \frac{R}{r_a}} \qquad (22)$$

To form an idea of the magnitude of this quantity, let R equal 200 feet and r_a equal 0.5 centimeter or 0.016 foot; then for r equal to one foot the positive charge is 0.56 of the negative charge; for six inches, 0.635; for three inches, 0.71; for one inch, 0.79; and for 0.016 foot, 1.0. If the density varies inversely as the distance, the incremental charges in incremental rings of equal thickness are equal, and if the radius of the space occupied by the charge in the downward leader is one foot, then the above values should be averaged over the radius, resulting in a positive charge about 70 per cent of the negative charge. If the radius is not so great, or the core has greater diameter or the charge is concentrated nearer the center (which is probably the case) this value would be larger.

References

1. DIRECT STROKES, NOT INDUCED STROKES, CHIEF CAUSE OF HIGH-VOLTAGE-LINE FLASHOVER, C. L. Fortescue. *Electric Journal*, August 1930, page 459.

2. TRAVELING WAVES DUE TO LIGHTNING, L. V. Bewley. AIEE TRANSACTIONS, volume 49, 1929, page 1050.

3. LIGHTNING PHENOMENA—I, C. F. Wagner, G. D. McCann. ELECTRICAL ENGINEERING, volume 60, August 1941, page 374.

4. PROGRESSIVE LIGHTNING, B. F. J. Schonland, H. Collens. *Proceedings* Royal Society, series *A*, volume 143, 1934, page 654.

5. PROGRESSIVE LIGHTNING—II, B. F. J. Schonland, D. J. Malan, H. Collens. *Proceedings* Royal Society, series *A*, volume 152, 1935, page 595.

6. PROGRESSIVE LIGHTNING—III, D. J. Malan, H. Collens. *Proceedings* Royal Society, series *A*, volume 162, 1937, page 175.

7. PROGRESSIVE LIGHTNING—IV, B. F. J. Schonland. *Proceedings* Royal Society, series *A*, volume 164, 1938, page 132.

8. PROGRESSIVE LIGHTNING—V, B. F. J. Schonland, D. B. Hodges, H. Collens. *Proceedings* Royal Society, series *A*, volume 166, 1938, page 56.

9. LIGHTNING TO THE EMPIRE STATE BUILDING, K. B. McEachron. *Journal* Franklin Institute, volume 227, February 1939.

10. FIELD INVESTIGATIONS OF LIGHTNING, C. F. Wagner, G. D. McCann, Edward Beck. AIEE TRANSACTIONS, volume 60, 1941, page 1222.

11. SHIELDING OF TRANSMISSION LINES, C. F. Wagner, G. D. McCann, G. L. MacLane, Jr. AIEE TRANSACTIONS, volume 60, 1941, page 313.

12. THE MEASUREMENT OF LIGHTNING VOLTAGES AND CURRENTS IN NIGERIA (part 2, 1938–39), F. R. Perry, G. H. Webster, P. W. Baguley. *Journal* of Institution of Electrical Engineers, England, January 1942.

13. LIGHTNING TO THE EMPIRE STATE BUILDING, K. B. McEachron. AIEE TRANSACTIONS, volume 60, 1941, September section, page 885.

14. THE LIGHTNING DISCHARGE, C. E. R. Bruce, R. H. Golde. *Journal* of Institution of Electrical Engineers, December 1941, page 487.

15. EXPERIENCE WITH PREVENTIVE LIGHTNING PROTECTION ON TRANSMISSION LINES, S. K. Wal-

Load Ratings of Cable—II

HERMAN HALPERIN
MEMBER AIEE

Synopsis: Since the author's 1939 paper,[1] further data have been obtained regarding the loading of underground power systems having impregnated-paper-insulated lead-covered underground cable. The results of the studies may be summarized as follows:

1. The factors affecting the setting of maximum safe conductor temperatures are so numerous that no fixed value (or values) can be assumed as applicable to all installations of a given design and size of cable in this country.

2. For some cables, the present temperature limits for normal day-in and day-out operation may be safely exceeded, especially for wartime conditions.

3. For emergency operation safe temperatures may be even higher than listed in the previous paper, especially for wartime operation.

4. Operation in wartimes at special temperatures will mean in some cases substantial shortening of the life of underground circuits and an accompanying increase in service interruptions.

5. Wartime increases in usual maximum daily loading and in load factors may cause large increases in duct and copper temperatures, even if past current ratings are maintained. These temperatures will sometimes exceed those in the present standards.

6. The safe temperature for emergency operation, particularly for extra-high-voltage cables, may be limited by the joints. Also, for all kinds of cable sufficient room must be provided to avoid mechanical damage of cable or joints in manholes with cable movements incidental to emergency loads.

7. Cracking of lead sheaths due to reciprocating cable movement into manholes may limit the temperature range for usual daily loading, but has little effect on the safe emergency loading.

8. For three-conductor solid-type cable, the insulation of the shielded type can safely withstand higher temperatures than the belted type, but the reverse is true as to the allowable daily temperature range with regard to its effect on the sheath in manholes.

9. Cable movement increases with length of conduit sections up to about 250 feet but shows little change with further increases in length up to 1,025 feet.

10. Changes in installation methods and the use of new types of repairs may help to mitigate troubles due to sheath cracking.

11. Copper shielding tape in three-conductor cable with relatively thin insulation has little effect in reducing the thermal drop from the conductor to the sheath.

12. In some cases considerable thermal advantage may be gained economically by making relatively deep installations of conduit.

13. In most cases, the maximum conduit temperature of 50 degrees centigrade given in the previous paper may be safely exceeded.

THE 1939 AIEE paper, "Load Ratings of Cable," by the present author

(*a*). Brought out mainly that the temperatures and allowable loading of underground cable during occasional emergencies could safely be considerably above the past accepted practices.

(*b*). Presented a large amount of detailed data on the limitations due to various factors for different underground cables and conduit systems.

The current war effort and attendant problems on conserving critical materials and labor have increased the importance of this general subject. This paper presents data that are extensions of investigations covered in the previous study and new investigations.

As a general proposition one cannot be dogmatic about the allowable temperatures for a given underground cable circuit. Factors affecting the decision include age and condition of the cable and accessories, effect of heat on the properties of the materials in the circuit, magnitude of usual daily temperature range, size of manhole and training of cable therein, frequency of emergency loading, past history of loading of cable, relation of temperature and temperature range for normal loading to temperature and temperature range for emergency loading, number of cables in conduit, allowable conduit temperatures, thoroughness of field inspection and maintenance work, desired life of the underground circuit, and desired reliability of service. One factor or set of factors may allow one loading while another factor or set of factors for the same cable may allow less loading.

The main purpose of this paper is to present data that, taken along with the data in the first paper and other available information, will aid in deciding the allowable normal and emergency loading for a given cable or system of cables of one design. The data are presented in three parts:

I—Limitations Due to the Insulation.

II—Limitations Due to the Sheath.

III—Heating Characteristics of Cables and Conduits.

I. Limitations Due to the Insulation

Effect of Heat Alone

Since the quality of wood-pulp paper as manufactured had improved since the period of 1920–30 when many tests were made in this country on the effect of high temperatures, it was decided to make further tests. In this series, samples of complete cables of recent manufacture were used in order to make the test conditions more nearly comparable to those encountered in service, instead of using paper tapes as had been usually done in the past.

The samples were 30-inch pieces of three-conductor 500,000-circular-mil solid-type cable from five manufacturers. The ends of each sample were cut square and immediately covered with lead foil which was bound to the sheath with fine copper wire. A lead cap, previously spun to fit the cable, was then placed over each end, beaten to close onto the cable, and sealed to the sheath by lead burning. Tests showed that this method of sealing practically eliminated contamination of the ends of the sample during preparation and also reduced to a negligible quantity the amount of air trapped between the seal and the cable end.

The samples to be aged were placed in ovens—some at 100 and some at 125

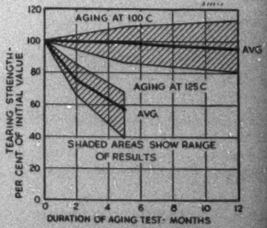


Figure 1. Effect of high-temperature aging on physical strength of impregnated-paper insulation (solid-type)

Paper 42-111, recommended by the AIEE committee on power transmission and distribution for presentation at the AIEE summer convention Chicago, Ill., June 22–26, 1942. Manuscript submitted April 6, 1942; made available for printing May 11, 1942.

HERMAN HALPERIN is assistant equipment and research engineer, Commonwealth Edison Company, Chicago, Ill.

The author is indebted to the following for assistance in the preparation of the paper: H. A. Adler, C. E. Betzer, O. B. Turbyfill, K. O. Bower, F. O. Wollaston, and D. D. Nagley.

---

dorf. AIEE TRANSACTIONS, volume 60, 1941, June section, page 249.

16. LIGHTNING INVESTIGATIONS ON TRANSMISSION LINES—VII, W. W. Lewis, C. M. Foust. AIEE TRANSACTIONS, volume 59, 1940, May section, page 227.

17. TRAVELING WAVES ON TRANSMISSION SYSTEMS (book), L. V. Bewley. John Wiley and Sons, Inc., New York, N. Y.

18. SHIELDING OF SUBSTATIONS, C. F. Wagner, G. D. McCann, C. M. Lear. AIEE TRANSACTIONS, volume 61, 1942, February section, page 96.

19. LIGHTNING PHENOMENA—III, C. F. Wagner, G. D. McCann. ELECTRICAL ENGINEERING, volume 60, October 1941, page 483.

20. SURGE VOLTAGE INVESTIGATIONS ON TRANSMISSION LINES, W. W. Lewis. AIEE TRANSACTIONS, volume 47, 1928, page 111.

21. KLYDONOGRAPH SURGE INVESTIGATION, J. H. Cox, P. H. McAuley, L. G. Huggins. AIEE TRANSACTIONS, volume 46, 1927, page 315.

Table I. Changes in Power Factors During Various Heating Periods for Experimental Oil-Filled Cable

Temperature Step—C	Weeks	Change in Power Factor							
		Room Temp (15 Kv)		Room Temp (95 Kv)		Elevated Temp (15 Kv)		Elevated Temp (95 Kv)	
		Minimum	Maximum	Minimum	Maximum	Minimum	Maximum	Minimum	Maximum
70	27	0.0001	0.0005	0	0.0013	−0.0007	0.0002	−0.0003	0.0014
80	24	−0.0001	0.0003	−0.0006	0.0005	−0.0004	0.0008	−0.0005	0.0005
90	115	−0.0009	0.0008	−0.0007	0.0005	−0.0014	0.0031	−0.0007	0.0013
100	34	−0.0007	0.0011	−0.0001	0.0003	−0.0032	0.0004	−0.0004	0.0006

degrees centigrade. The effect of aging upon the paper insulation was determined from measurements of

(*a*). The radial power factors of the tapes

(*b*). The tearing strength of the tapes.

The power-factor measurements were on circular areas of single tapes, one half inch in diameter and were made at 60 degrees centigrade and 50 volts per mil. For each sample of cable the power factor of the tapes was measured along a radial path from one conductor to sheath.

The measurements of tearing strength were made with an Elmendorf tearing tester, four to eight pieces of tape being placed together and torn longitudinally at the same time. The tapes were tested immediately after removal from the cable under normal room conditions of temperature and humidity. This seemed warranted since a preliminary investigation showed that variations in relative humidity between 20 and 50 per cent had showed that variations in relative humidity between 20 and 50 per cent had no appreciable effect on the test results so long as the tapes were tested soon after removal from the cable without removing the impregnating oil.

The changes in tearing strength of the paper tapes after various amounts of aging are shown by Figure 1. These data show that for most modern cables, subjection to a temperature of 100 degrees centigrade for 12 months produces negligible depreciation in physical strength of the paper tapes, the average decrease in tearing strength being only 4.6 per cent, as compared with a decrease of 40 per cent in 10 months for the tests reported in 1926–29 by the Massachusetts Institute of Technology.[2]

At 125 degrees centigrade, however, the average deterioration in tearing strength after five months' aging was 43 per cent. At this temperature the rate of decrease in tearing strength of recent paper appears to be of the same order as was found in the 1926 tests. The recent and other tests have shown that tapes which have decreased more than 50 per cent in tearing strength are usually so brittle that their useful life in cable may be considered ended.

The effects of aging on the average power factors of the tapes are shown by Figure 2. For some samples, heating at 100 degrees centigrade for 12 months or at 125 degrees centigrade for five months produced no appreciable change in average power factor. For the best samples, the radial power-factor curves also remained completely flat throughout the tests, with no indications of the common upturns adjacent to the conductor and sheath. For the worst sample, the power factor of all conductor tapes increased from 0.0025 to about 0.01 after 12 months' aging at 100 degrees centigrade; and after five months at 125 degrees centigrade, some of the inner tapes had power factors as high as 0.05. In general, such increases in power factor would not result in a cumulative heating condition for solid-type cable within the present general range of usage but might lower the carrying capacity of the higher-voltage cable by a few per cent.

These data indicate that from the standpoint of deterioration due to heat alone, the maximum safe temperature for modern impregnated-paper insulation is limited by depreciation in physical strength of the tapes rather than by changes in electrical properties. For low-voltage cables where the electrical stresses are too low to have any appreciable effect, therefore, it appears that the insulation could be safely operated almost continuously at conductor temperatures of 90 or 100 degrees centigrade without excessive deterioration; and temperatures as high as 125 or about 140 degrees centigrade during emergencies would be reasonable. The higher limits (100 and 140) would apply for wartime conditions. "Emergencies" or "emergency loading" in this paper means extra heavy loading that would occur on the average of one day per year.

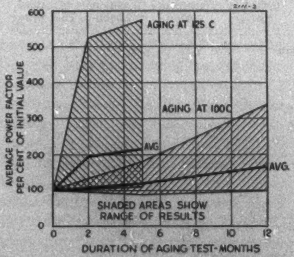


Figure 2. Effect of high-temperature aging on power factor of impregnated-paper insulation as determined from averages of radial power-factor curves at 60 C

Oil-Filled Cable

The 1939 paper included data on tests of three 1,000-foot installations of experimental single-conductor oil-filled cable that was being subjected to high temperatures. This cable was installed in conduit and subjected to 76 kv to ground by means of a tap to a 132-kv three-phase overhead line. After a net time of $8^{1}/_{2}$ years of testing, the cables were removed, and extensive investigations were made on samples from various locations.

The three cables are identified as *R*–0.386, *S*–0.400, and *T*–0.500, the number in each case being the insulation thickness in inches. Cable *R*–0.386 had oil feed through both a hollow core and flutes in the inside surface of the sheath, while the other two cables had oil feed through a hollow core only.

As indicated in the earlier paper, the conduit in which these cables were installed was in a very abnormal location and had unexpectedly large variations longitudinally in heating characteristics. As a result, the cables were subjected to widely different temperatures longitudinally. The temperatures were determined from direct resistance measurements of the average conductor temperatures at two- to three-week intervals and by measurements of longitudinal variations in duct temperature on two dates. As a result of this nonuniform temperature distribution, samples of cable were available that had been subjected to maximum conductor temperatures ranging from 95 to 140 degrees centigrade and to periods of heating to 100 degrees centigrade or higher ranging from 0 to 27,000 hours. The heating to which each of the three cables was subjected at the hottest and coolest locations along the conduit is summarized in Figure 3.

After $8^{1}/_{2}$ years of testing, cable *R*–0.386 failed because of overheating at the hottest part of the duct run. Figure 4 of the previous paper shows how the

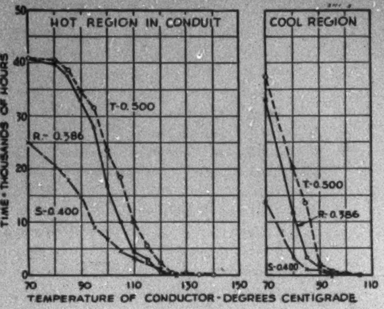


Figure 3. Time of heating at or above various temperatures for experimental oil-filled cables operated at 76 kv to ground

power factors of the cables varied during the tests. Cable *R*–0.386 was electrically stable up to the time of failure, and radial power factors measured on samples of this cable taken from the coolest and the hottest parts showed practically no changes due to all the testing.

On the other hand, cable *T*–0.500 had developed considerable increase in power factor. A sample from the cooler part of the conduit showed power factors at 60 degrees centigrade of the tapes of 0.03 near the conductor and near the sheath, with values dropping down to 0.005 in the middle of the insulation, while a sample from the hotter part of the length had a power factor of 0.025 or 0.03 radially through the insulation. The primary trouble with this cable was the development of an excessive amount of sheath expansion, which increased the internal sheath diameter by 0.06 to 0.26 inch ($2^1/_2$ to 12 per cent) with the greater expansions occurring at the hottest regions. This allowed core oil of high power factor to be drawn radially through the insulation. These excessive amounts of sheath expansion were due mainly to the very high sheath temperatures encountered in these tests and to the fact that the type of alloy used for the sheaths has abnormally high rates of creep, especially at the higher temperatures. Fracture is likely to occur after expansions of four or eight per cent in sheaths, except that the percentage is lower for calcium-type sheaths. This very unusual combination of very high power factors for the core oil and excessive sheath expansion is unique and, according to world-wide experience, is not at all to be expected in commercial installations.

An interesting phenomenon brought out by these tests, although not directly related to the effect of temperature on the insulation, was the fact that, at locations where the gap between the sheath and the insulation resulting from the sheath expansion exceeded about 0.15 inch, severe electric discharges occurred between the outer surface of the unshielded insulation and the inside surface of the sheath.

Table II. Effect of Temperature Steps Upon Ionization Factor

Temperature Step—C	Percentage of Cables Tested Showing Increases in Ionization Factor Over Initial Value of: 0.0005 or less	0.0006 to 0.0010	0.0011 to 0.0050	0.0051 to 0.0100	0.0101 to 0.0200	0.0201 to 0.0300	Percentage of Cables That Failed
Nine Shielded Cables With 108 to 143 Mils of Insulation							
60	100						
80	56	33	11				
100	34	33	33				
115	56	22	22				
Five Shielded Cables With 90 to 105 Mils of Insulation							
60	80		20				
80	40	20	20	20			
100			60	20	20		
115			40	40	20		
Six Unused Belted 13-Kv Cables Made in 1937–39							
60	17	50	17	16			
80	17	17	17	16	33		
100			50		33	17	
115				17	50	33	
Five Used Belted 13-Kv Cables Made in 1924–28							
60			20	20	60		
80			20		80		
100				40	20		40
115				20		20	20

These discharges burned numerous deep pits in the inside surface of the sheath and burned holes through the outer two to five tapes of the insulation. No visible evidence of damage was found below the sixth tape from the outside, and all indications were that the discharges had little if any effect on the power factor of the insulation below that tape. These cables were subjected to lightning and switching surges having crest voltages as high as 600 kv, and it is possible that these surges caused the initial breakdown of the oil gap. From other investigations, it seems possible also that gas was evolved from the paper during the very high temperatures.

The results of measurements of the tearing strength of the paper insulating tapes from samples of cable that had been subjected to various temperatures are summarized in Figure 4. During preparation of this figure, the temperature treatment to which each sample had been subjected was reduced to the equivalent temperature for a period of 12 months, using the assumption that the rate of deterioration in physical strength of the insulation would double for each eight degrees centigrade increase in temperature. Since the tearing strength of the tapes when new had not been measured, the initial values for each cable were taken as the average results for all samples of tape that had been subjected to maximum temperatures of lower than 85 degrees centigrade.

A definite but rather gradual decrease in tearing strength with increasing temperature is shown by these data. The indications are that temperatures equivalent to at least 117 degrees centigrade for 12 months would be required to produce a reduction in tearing strength of 50 per cent (approximate allowable limit for useful life, especially in connection with removal and reinstallation).

The failure in cable *R*–0.386 occurred at a location where the conductor temperature had been at least 140 degrees centigrade for approximately 100 hours prior to the failure. The examination

Table III. Increase in Solid Losses During Aging Tests of Three-Conductor Cable

Cables	Percentage of Cables Tested Showing Maximum Increases in the Average of Radial Power Factors at 60 C of: 0.001 or Less	0.0011 to 0.0050	0.0051 to 0.010	0.0101 to 0.0272
Shielded*	23	54	23	
Belted	45	45		10

* Reduced insulation thicknesses.

indicated that the final failure was of the cumulative heating type, but the radial power factors of adjacent samples were low as in new cable. The exact mechanism of failure is not well understood, but it seems likely from other Chicago tests that the high temperature may have resulted in the evolution of some gas from the paper. The failure would then have been initiated by ionization of the gas with subsequent development of a cumulative heating condition.

Aging tests of six oil-filled experimental cables with a conductor size of 500,000 circular mils and 450 mils of insulation have been in progress since 1936 in an indoor laboratory. The cables have been subjected to various steps of heating up to 100 degrees centigrade as shown in Table I. Heating was applied partly continuously and partly in daily cycles. During some test periods a test voltage of 95 kv to ground was applied, and some test periods were without superimposed voltage. As Table I shows, the changes of the insulation, judging from the results of power-factor measurements, were small (0.003 or less).

These tests were continued at an average copper temperature of 110 degrees centigrade. After nine weeks at this temperature step without voltage, 95 kv was applied, and then a failure occurred in one joint due to the effect of overheating.

Experience in aging tests with overvoltage and heating currents on both solid and oil-filled types of cables for extra high voltages has shown in many cases that trouble caused by overheating may develop more readily in joints and potheads than in the cable. This may be due to the use of large amounts of insulating materials, which decrease the dissipation of heat, or due to loosening of metal parts, or other causes. This finding emphasizes the need for careful study of the limitations of the accessories in the determination of emergency ratings for extra high-voltage cables.

From a consideration of the results of these investigations together with other earlier data, the following conclusions may be drawn:

1. Oil-filled cables may be subjected continuously to conductor temperatures of the order of 100 degrees centigrade for at least 30,000 hours without producing any serious increases in power factor.

2. The maximum safe temperature for the *insulation* of oil-filled cable for long periods of time is probably limited by deterioration in the physical strength of the paper rather than by changes in electrical properties. From this standpoint the cable will withstand 115 degrees centigrade for one year or 100 degrees centigrade for at least 30,000 hours.

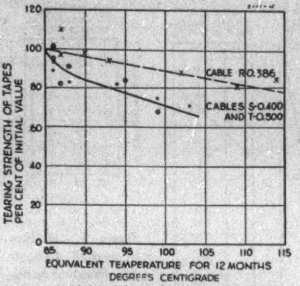


Figure 4. Effect of temperature on tearing strength of paper tapes from experimental oil-filled cables

(The preceding two conclusions are of considerable interest in connection with other oil-impregnated insulation, especially that normally used in transformers, but it must be noted that in oil-filled cable the amount of air present is relatively very small.)

3. At some temperature that is definitely well above 100 degrees centigrade and is probably between 120 and 140 degrees centigrade, there is danger of electrical failure of the cable occurring without previous changes in power factor. Joints and potheads may give trouble at lower temperatures.

4. If oil-filled cables are operated for long periods of time at sheath stresses or temperatures in excess of those for which they were designed, excessive sheath expansion may occur.

Maximum allowable temperatures for oil-filled cable may, therefore, be as high as follows: for usual conditions—85 and 105 degrees centigrade for normal and emergency loading, respectively; for wartime conditions—95 and 115 degrees centigrade for normal and emergency loading, respectively.

Aging Tests of Three-Conductor Solid-Type Cable

A great amount of information on the combined effect of voltage and temperature upon the stability of solid-type cables has been obtained in aging tests of three-conductor cables for operation at 9 or 12 kv, three-phase. As described in the previous paper, the tests consisted essentially in subjecting test lengths of 100 feet or more between pressure-tight terminals for four weeks to 24 kv, three-phase, continuously with 20 superimposed load cycles ranging in maximum temperatures from 60 to 115 degrees centigrade. The compact terminals were filled with heavy mineral oil, such as is used in solid cable.

Since the previous paper, 23 more cables have been tested. Since the inception of these series of tests in 1935, a total of 30 cables has been tested with pressure-tight potheads; five cables have been tested with open potheads, and four cables with old rosin compound made prior to 1920 have been subjected to similar but milder tests. Of the 30 cables tested with pressure-tight potheads, 17 were shielded cables and 13 were standard belted 13-kv cables with 141 mils of conductor insulation and 78 mils of belt insulation.

The net insulation thicknesses of the shielded cables ranged from 90 to 143 mils. Added to these walls of insulation were in most cases two or three special shielding tapes over the conductor and one special shielding tape over the insulation. The special shielding tapes were either metalized paper tapes or carbon black tapes. Based on the results of these aging tests, such as stability throughout tests with 125 mils of insulation, an insulation thickness of 141 mils including special shielding tapes has been adopted as standard for operation on the Chicago 12-kv system as compared with net insulation thickness of 172 mils recommended by the Association of Edison Illuminating Companies Cable Specifications.

Table II shows the effect of the standard aging tests with pressure-tight potheads upon the development of ionization in 25 representative cables. A great superiority in stability of the shielded over the belted cables is evident in spite of the reduced insulation thicknesses of the shielded cables, especially if the five cables with extremely thin insulation are eliminated from comparison. The nine shielded cables with insulation of 108 mils or more showed less increase in ionization than 0.005 throughout these aging tests. As was expected, more severe deterioration occurred in the cables with extra thin insulation. In many cases shielded cables showed only small increases in ionization or even decreases in the 115-degree-centigrade step after deterioration in the 80- and 100-degree-centigrade steps, possibly due to redistribution of compound at the extra high temperatures.

Belted cables showed, in general, progressing deterioration in successively higher temperature steps. Also, higher percentages of the cables tested reached greater degrees of deterioration. However, in contrast to shielded cables, they showed, in general, considerable recovery during days with voltage only and during cycles at only 60 degrees centigrade following the high-temperature cycles.

Failures occurred only in used belted cables and only in the 100- or 115-degree

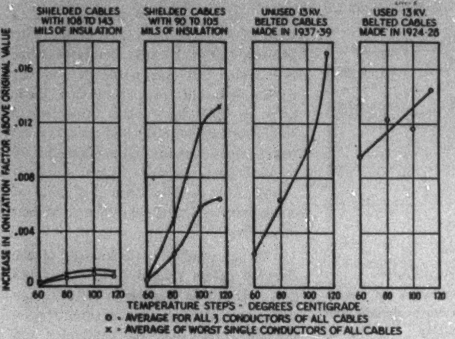


Figure 5. Increase in ionization factor at room temperature during aging tests

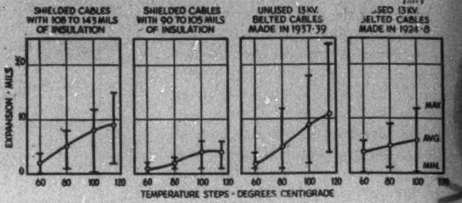


Figure 7. Increase in over-all diameter of three-conductor cables after various temperature steps in aging tests

step. The three failures that occurred were all due to ionization.

Figure 5 gives some indication of the rate of deterioration with increased temperatures, as it shows the average increase in ionization above the initial values for all cables in the various temperature steps. Since in many cases where deterioration occurred in shielded cables only one conductor was affected, both the increases for the worst conductors and the average increases for all three conductors are shown for the shielded cable. For the belted cables, only the average increases for all three conductors are given. The very small effects of the various temperature steps and especially of the 115-degree step are apparent for shielded cables with more than 108 mils of insulation. For unused belted cable the curve becomes progressively steeper with higher temperatures. The used belted cables tested showed greater changes but a flatter slope; the reasons for the shape of the curve are not completely clear.

Taking all data into consideration, and noting that the tests were made at twice operating voltage, one should not expect serious deterioration due to ionization on lines containing only cables made recently at emergency temperatures of 100 degrees centigrade or somewhat more; but some serious deterioration may occur at such temperatures on lines containing cables made, say, 14 or 18 years ago. The margin of safety is greater for the shielded cables. This conclusion agrees fairly well with the emergency temperature of 98 degrees centigrade recommended for 12-kv operation in the AEIC Simplified Practice Schedule.

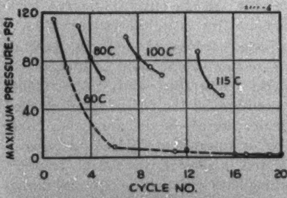


Figure 6. Maximum pressures in various temperature steps during aging tests of a typical sample of 500,000-circular-mil three-conductor shielded cable

As discussed in the previous paper, thermal failures (cumulative heating) due to steep dielectric loss-temperature curves may occur at even moderate temperatures in local spots of some old rosin cables, but such failures should not be expected in cables made later, even at temperatures as high as 120 degrees centigrade, except under abnormal conditions.

War conditions will necessitate increasing use of American wood pulp for insulating tapes instead of the Swedish wood pulp used heretofore. For this reason, comparative aging tests were made on samples insulated with Swedish wood pulp and similar samples insulated with American wood pulp. The samples were purposely made with very thin insulation in order to obtain definite deterioration. While, as expected, all samples showed serious deterioration, the test results as well as examinations and tests after completion of the aging tests showed no appreciable difference in stability of the two types of paper. Experimental lengths of various makes with reduced insulation thicknesses with American and Swedish wood-pulp paper insulation have been installed on heavily loaded 12-kv lines and will be removed after a few years of service for further comparisons.

Compared with the effect of these overload tests on ionization, the effect on the solid losses of the cables was small. As Table III shows, the great majority of the samples showed only small increases in solid losses, and only a few showed moderate or serious increases. It should be noted that the increases shown are the maximum changes; that is, the portions of the test lengths were selected that developed hot spots or showed otherwise the greatest degree of deterioration. The conclusion may be drawn that occasional overloads will not affect seriously the dielectric losses of the impregnated tapes themselves.

In accordance with the results of the electrical measurements, visual examination of aged cables gave evidence that deterioration was mainly due to ionization. Deteriorated samples showed dryness and wax, and in the case of more deteriorated belted cables carbon also.

Figure 6 shows typical pressure readings during the 20 heat cycles in the standard aging tests. The pressures dropped sharply in successive cycles of the same maximum temperatures. In general, the highest pressures occurred in the first week of 60- or 80-degree cycles rather than in subsequent cycles to higher temperatures. These data lead to the conclusion that severe overloads applied to cable that previously carried heavy loads will not produce excessive pressures, assuming that joints are not oil-filled and connected to reservoirs.

The expansion of the sheath produced by the heat cycles is summarized in Figure 7. As should be expected, a comparison of this figure with Figure 5 suggests a relation between sheath expansion and progress of ionization. These expansions are not sufficient to cause fracture in sound sheath. However, there were some cases in these aging tests where leaks occurred in the thin portions of eccentric sheath in old cables. None of these leaks occurred before the 100-degree step. These observations in the aging tests suggest that it must be expected that in service sudden overloads may open up cracks in thin or defective sheaths.

It is difficult to generalize as to allowable temperatures for three-conductor cable for operation at about 12 kv, mainly due to the variety of cables in service. It

Table IV. List of Cables on Which Movement Studies and Measurements Have Been Made in Chicago

			Conductor			Specified Thickness—Mils					
	Kv Between Conductors					Insulation					
No.	Rated	Operating	Size—Circular Mils	No.	Type of Cable and Conductor	On Conductor	Belt	Sheath	No. of Field Measurements Taken	A/W* Ratio	Outside Diameter in Inches
1	5	4	375,000	3	Belted, ordinary	94	63	109	269	0.144	1.98
					Belted, "compack"	94	63	109		0.148	2.05
2	5	4	375,000	4	Belted	94	63	125	—	0.148	2.35
3	13	4	350,000	3	Belted	141	78	125	11	0.119	2.27
4	13	12	500,000	3	Belted, ordinary	141	78	125	470	0.138	2.53
					Belted, "compack"	141	78	125		0.142	2.44
5	9	12	500,000	3	Shielded, "compack"	141	—	117	69	0.149	2.31
6	13	12	350,000	3	Belted	141	78	125	28	0.119	2.27
7	35	12	350,000	3	Belted	297	109	141	22	0.083	2.98
8	69	66	2,100,000	1	Solid, segmental conductor	688	—	156	30	0.117	3.48
9	69	66	2,100,000	1	Oil-filled, hollow core	315	—	141	82	0.145	2.82
10	69	66	1,000,000	1	Solid, standard stranding	688	—	141	17	0.087	2.87
11	69	66	750,000	1	Solid, standard stranding	750	—	141	146	0.0696	2.85
12	138	132	1,100,000	1	Oil-filled, hollow core	719	—	156	45	0.0772	3.30
13	132	132	600,000	1	Oil-filled, hollow core	719	—	100+90	26	0.0426	3.22
14	132	132	600,000	1	Oil-filled, hollow core	719	—	250**	7	0.0445	3.03
15	132	132	600,000	1	Oil-filled, hollow core	719	—	156	2	0.0533	2.89
16	138	132	250,000	1	Oil-filled, hollow core	560	—	133	31	0.0325	2.44

* A/W ratio is the ratio of the total copper area in the cross section of the cable expressed in circular inches to the weight per foot of cable expressed in pounds.

** Contains fluted oil channels 94 mils deep.

seems, however, from data in this paper that from the standpoint of the insulation (including radial expansion of the sheath), the normal and emergency temperature limits are, respectively, about as follows for limiting cable types: for modern shielded cable—85 and 110 degrees centigrade; for 1920–25 belted cable—75 and 90 degrees. For single-conductor cable, the limits may be the same as for shielded cable. For wartime conditions, the temperatures could be increased perhaps seven degrees for normal daily loading and ten degrees for emergency loading. For some old cables with relatively heavy insulation (usually containing relatively large amount of voids), allowable emergency temperatures during wartimes might well be 120 degrees or even higher, depending on cumulative heating characteristics and condition of sheath. With the very high cable temperatures, troubles may occur with the joints due to expansion of compound or movement against manhole wall, and so forth.

II. Limitations Due to the Sheath

The main objective in studying sheath life is to find out how to get a reasonably long life out of the sheath under any and all loadings up to the maximum that the insulation will stand. The loading and resulting temperature ranges determine the cable movement, which in turn determines the sheath life in the manholes.

The earlier paper presented some data on magnitudes of cable movement found in the field and theories relating thereto. In addition, some information was given regarding the relation between sheath life and cable movement as determined by the influence of cable size and construction, by sheath material, and by installation conditions in ducts and manholes. Further data and discussions on movement and sheath life are given herein.

As indicated in the previous paper, most underground circuits having material variations in daily temperatures—which is the usual case—are limited in rating for day-in-and-day-out (normal) loading more by the sheath than by the insulation. For example, the insulation might limit the normal daily temperature to 85 degrees centigrade for a given cable, while for the accompanying conduit temperatures and character of daily variation in load, the resistance of the sheath to cracking in the manhole may limit the copper temperature to 77 degrees. The problem of sheath cracking in manholes is affected by so many variables that it is hard to lay down any definite rules and positive numerical conclusions. Nevertheless, a great deal of data have been gathered which lead to more or less general conclusions that may serve well in guiding design and operating practices.

Laboratory Tests

Laboratory tests are mainly useful in determining the general engineering relations between sheath life and cable movement for different variables. The tests relating sheath life to magnitude of movement and to cable training in the manhole have all been made on pieces of cable 4 (Table IV) from one length which had copper-bearing lead sheath and compack conductors. The tests were made in the dummy manhole described in the previous paper with the cable fireproofed in the standard manner there described.

The relationship between sheath life and cable movement is shown in Figure 8. Where the movement at each duct mouth is 0.75 inch or less, the life appears to be inversely proportional to roughly the 1.5 power of the movement. For movements of over 0.75 inch the life is less than would be indicated by this relation; the limited data available indicate that in this range the life is inversely proportional to something more than the square of the movement.

In one set of tests in the dummy manhole, the one variable was the offset between the center line of the joint and the center line of the cable in the duct. The numbers of cycles to sheath cracking for offsets of $9^1/_2$, 19, and $28^1/_2$ inches were, respectively, 3,100, 6,800, and 10,000 cycles. Apparently, then, increasing the offset is very helpful in lengthening the life of the sheath. Of course, there is the attendant problem of finding enough room for the wider manholes thereby required. Also, as indicated in the previous paper, the magnitude of the daily movement at the duct mouth is affected by the restraint of the cable in the manhole; and with large offsets this restraint would be decreased somewhat to nullify part of the apparent advantage in longer offsets.

In another set of tests the one variable was the length of the manhole, the offset being 19 inches. For lengths of $7^1/_2$, 10, and 13 feet between duct mouths the corresponding number of cycles to sheath cracking was, respectively, 4,700, 6,800, and 6,900 cycles. Apparently, if the offset is kept constant, increasing the length of the manhole is of material value only

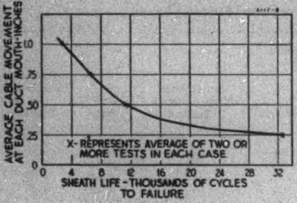


Figure 8. Relation of sheath life in dummy manhole to cable movement

up to a certain point. Beyond that point, apparently, the strain on the sheath is not much affected.

A number of tests have been made to determine the effect of fireproofing on the life of the sheath. The standard fireproofing compound in Chicago is a cement, sand, and asbestos mixture. The standard method is to apply this mixture over plain felted asbestos tape which is one-eighth inch thick and wrapped in a single butted layer over the sheath. With such fireproofing the life of the sheath in the dummy manhole does not seem to be much different, according to tests, from the life with no fireproofing at all. Omission of the asbestos tape, however, resulted in reductions of about one third in the life in the dummy manhole in a pair of tests at a movement of 0.75 inch and in a pair at a movement of 0.25 inch. Apparently, a thick solid fireproofing will shorten the life of the sheath, particularly if the fireproofing itself cracks in service.

In 1940 it was decided that the tests in the dummy manhole apparatus should be supplemented by tests on short pieces of cable and on small strips of sheath in which the magnitude of the cyclic deformation of the sheath could be more accurately controlled than is possible in the dummy manhole test. It was thought that the better reproducibility and relative cheapness of tests of this type would make them preferable for comparative tests of many variables in materials and conditions. In order that the results of such a test be applicable to service conditions, however, it is necessary that the type and the magnitude of the deformation be approximately the same as occur in service or in the dummy manhole test. Therefore, an investigation was started to determine the cyclic deformation of the sheath at various locations along the cable during tests in the dummy manhole apparatus. The investigation was later expanded to obtain deformation data for use in analyzing the effect upon the sheath life of variations in installation conditions and type of cable.

After trials of different schemes, it was found that the most satisfactory method of determining the cyclic sheath deformation was to measure the change in distance between the ends of small copper rivets soldered to the sheath at three-inch intervals along both the front and the back of the cable. A small conical hole was drilled in the end of each rivet, and the distance between the holes in adjacent rivets was measured with a dial gauge reading to the nearest 0.001 inch, thus giving an accuracy of ±0.03 per cent. The tests were made on fireproofed cables.

The early tests showed that the deformation at any given location along the cable might vary considerably during a given dummy manhole test. As a result, it was found necessary to measure the sheath deformations after various periods during the test and to take the average of all measurements at a given location as being the effective deformation at that location.

Figure 9 shows the variation in sheath deformation or strain along the cable for a typical test on cable 4 (Table IV). As shown by Table V, the maximum sheath strain for this cable under these conditions ranged from 0.35 to 0.42 per cent, and usually occurred in the three-inch section adjacent to the joint wipe. The results of tests on the same type of cable under different training conditions are also shown on Table V. The equivalent lives in the dummy manhole test and the maximum sheath strains were in all cases in the correct relative order; that is, higher deformations give shorter lives, but the number of measurements is insufficient to determine the relation between sheath strain and life.

When shielded cables were adopted as standard for use on the 12-kv system in Chicago in 1940, some tests in the dummy manhole were made to see whether the lives under bending were of the same order as for the belted cables which were previously used. As shown by Table V, the life of the shielded cable with standard training and 0.75-inch movement at each duct mouth was only about 4,300 cycles, or 63 per cent of the average life for all comparable tests of belted cable. With 0.50-inch movement at each duct mouth, the life of the shielded cable was about 5,400 cycles or one half of that for belted cable.

For both values of movement the measured sheath strains were slightly higher for the shielded than for the belted cables, but the differences were insufficient to account for the differences in equivalent lives in the dummy manhole test. The shorter lives of the shielded cables appear to be connected with the fact that the sheaths bulge and wrinkle under the repeated bending to a much greater extent than do those of the belted cables. Preliminary investigations suggest that this may be due to

(*a*). A tendency for the metal binder tape to concentrate the sheath deformation at certain spots.

Figure 9. Variations in sheath strain along cable for typical 13-kv belted cable in ten-foot manhole with 0.75-inch movement at each duct mouth

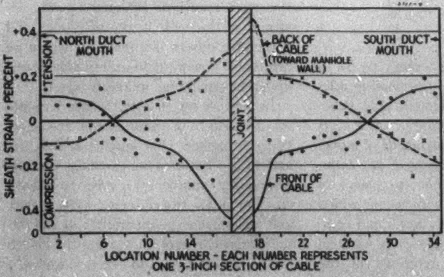
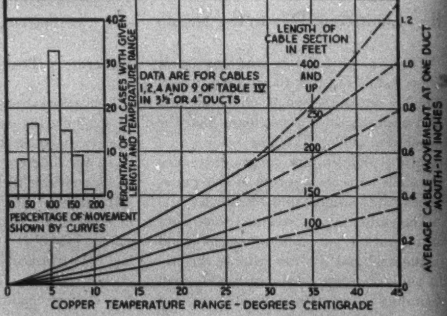


Figure 10 (right). Curves for estimation of average movement and distribution of measurements with respect to curves



(*b*). The failure of the usual metal binder to hold the conductors as firmly together as does the belt on a belted cable.

(*c*). The tendency for the slightly thinner sheaths used on the shielded cables (117 mils as compared to 125 mils on the belted cables) to wrinkle more easily.

The effects of these factors are being investigated further in an effort to determine what changes in construction of the shielded cables will be necessary to obtain longer sheath lives.

Table V. Tests in the Dummy Manhole Apparatus of Three-Conductor Cables on Which Sheath Strains Were Determined

Type of Cable	Manhole Length—Feet	Cable Training	Movement at Duct Mouth—Inch	Maximum Sheath Strain—Per Cent	Cycles to Sheath Failure
Belted	10	Standard	0.75	0.42	7,080
Belted	10	Standard	0.75	0.35	7,520
Belted	7.5	Standard	0.75	0.60	5,620
Belted	7.5	Standard	0.75	0.70	3,900
Belted	10	Joint blocked to prevent lateral movement	0.75	0.33	8,400
Belted	10		0.75	0.33	8,400
Shielded	10	Standard	0.75	0.45	4,310
Shielded	10	Standard	0.75	0.60	About 4,000
Shielded	10	Standard	0.50	0.50	4,970
Shielded	10	Standard	0.50	0.33	5,740

Field Data

The theory of cable movement, without which field data on movement are largely unintelligible and unusable, has been extended somewhat and brought into better agreement with the field data since the previous paper was published. The most interesting discovery is that long lengths of cable do not move any more than short lengths, within certain limitations. The graph in Figure 10 illustrates the present findings based on field measurements. For copper temperature ranges up to 25 degrees centigrade, all lengths in excess of 250 feet have only as much movement as a 250-foot length. This effect is probably due to snaking and anchoring of the cable in the duct or to duct friction.

The curves in Figure 10, which are almost entirely empirical, can be matched fairly well by using in formula 2 of the previous paper a value of D, the duct-friction coefficient, equal to about 1.6. This happens to be about the right value for the static coefficient of friction of cable in precast concrete duct, indicating that static friction, as well as complete anchoring, may be an important factor in limiting cable movement. When the coefficient of sliding friction, about 0.5, is used in the formula, the calculated movements are much higher than those found in service and exemplified in Figure 10 and, of course, would be still higher with sliding friction coefficients of 0.2 or 0.3, such as are sometimes reported. One case is known where the ducts are clogged with fine silt for several lengths on one part of a heavily loaded line, while on other parts of the line the ducts and cable are identical but free of silt. The daily range of movement at the duct mouth in this latter part of the line is about three times as great as in the silted ducts.

These points are strikingly illustrated by observations on a 500,000-circular-mil single-conductor 69-kv line having lengths up to about 1,000 feet in four-inch ducts. The data, for which the author is indebted to Cincinnati Gas and Electric Company, are given in Table VI. The lengths on which measurements were taken, given in column 1, range from 529 feet to 995 feet. In column 2 the measured total movement for both ends of each length for a copper temperature range of 38 degrees centigrade are given. It is especially interesting that the longer lengths had no more movement than the shorter lengths; in fact, the shortest length moved more than the longest length. The second interesting point is that, as shown by column 3, the observed movement was only about as much as would have been produced by 410-foot lengths, according to calculations using formulas for determining cable movement given in the previous paper. The remainder of each length was apparently immobilized by anchoring. The length of cable that actually moved, 410 feet, is somewhat longer than the corresponding value of 250 feet found for the cables studied in Chicago. One probable reason for this is that the Cincinnati cable was not fireproofed at the time of the field observations, whereas all Chicago cables are fireproofed. The absence of fireproofing increases the movement noticeably.

Data obtained early in 1942 for the Cincinnati line (with fireproofing on the cable in the manholes) showed for daily copper temperature ranges of about 18 degrees centigrade that the range of movement at the duct mouths was almost the same for 250- and 496-foot lengths as for 850- and 981- and 1,025-foot lengths. Also, it was found that slopes of six per cent for some of the conduit did not affect the daily movement.

Similar data on relatively small movement have been furnished by the Montreal Light Heat and Power Consolidated for its recent installation of 650,000-circular-mil 120-kv oil-filled single-conductor cable. Lengths of 682 to 960 feet had movements of $^{1}/_{8}$ to $^{7}/_{16}$ inch at a duct mouth for daily copper temperature ranges of 16 to 22 degrees centigrade. The largest movement was found for the 682-foot length and occurred with the 22-degree range.

The dashed portions of the curves in Figure 10 are for daily temperature ranges exceeding 25 degrees centigrade, which seldom occur in service. For this reason this portion of the curves is not well verified by field data. However, it can be said that the actual movements measured in Chicago have on the average been no greater than indicated by the curves. Any revision in this portion of the curves is expected to be toward smaller values of movement, especially for the longer lengths exceeding 250 feet.

The probability curve in the left-hand corner of Figure 10 gives the relation between the actual field data and the plotted curves. It may be seen that 46 per cent of all the field data are within 25 per cent above or below the values shown in the graph, while a few cases may be 100 per cent above or below the graph. Of course, it will be the lengths that have more than average movement that will tend to have relatively short lives before cracking of the sheath in the manhole. A rough analysis indicates that some lengths will start to crack after a period equal to one fourth or one third of the average life of the cable on the line, and after a period equal to one half the average life about one sixth of the original lengths will have cracked. If each length is replaced as it cracks, the indications are that the rate of cracking will increase up to a period equal to about two thirds the average cable life, after which it will tend to level off or even decrease somewhat. If the sheath limits the life of the line to about 20 years, for example, then the annual rate of sheath cracks may become 50 or 75 per 1,000 lengths.

As indicated by the title of Figure 10, no significant difference has been found in the movement in service of cables of the sizes indicated operating at 4, 12, and 66 kv. This is in accordance with expectations, since the ratio of conductor cross section to total cable weight is about the same for all these cables (see Table IV), and the movement should, therefore, be about the same.

Cable of a given size is found to have about as much movement in 3.5-inch duct as in four-inch duct, and probably also in three-inch and five-inch duct, although this latter is not well verified. Since lengths of cable in service are usually at least 250 feet long, limitation of the movement by anchoring is always a factor, and the negligible influence of duct size found to date indicates perhaps that anchoring occurs about as readily in large ducts as in small ones. Another way of looking at it is that the coefficient of either static or sliding friction is not much different for a cable in a large duct than in a small one, provided the cable can be installed in the smaller duct without actually jamming or tending to jam. The movement would therefore be expected to be about the same in both cases. Similarly, according to scanty data, the material of the duct does not seem to make much difference.

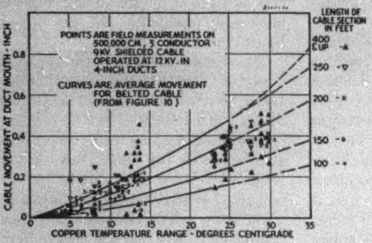


Figure 11. Comparison of movement of lengths of shielded cable with average movement of belted cable

It has been suggested that three-conductor cable constructed with a short lay should move substantially less than similar cable with a long lay. No significant difference has been detected based on field measurements on the usual run of belted cable and on some belted cable made some years ago with relatively short lay. These cables, of course, had the belt insulation and sheath tightly around the conductors. If, on the other hand, the three-conductor cable is of the shielded type, and the binder and sheath are not particularly tight, then the cable in ducts may move less with shortening of the lay. Even in this case there would still remain the thermal expansion of the lead sheath itself, which might produce substantial movement of the sheath at the duct mouth. Also, no significant difference has been found between cables having "compack" sector conductors and those having ordinary sector conductors.

It is not claimed that the factors discussed in the foregoing three paragraphs have no effect at all. What is meant is that their effects are not large enough to be detected individually in data on cable in service, because of the inherent variability of such data. For example, a single length of cable is often found to have considerable movement at one end and practically none at the other. The cause may be suspected to be variable friction along the duct, but it is not practicable to prove it. Similarly, the various other factors may be suspected, but no way has yet been found of isolating and proving them.

Considerable data have been obtained on cables not covered in the curves of Figure 10. Figure 11 shows data for 500,000-circular-mil three-conductor shielded cable operated at 12 kv (cable 5, Table IV). Such cable has about the same ratio of copper cross section to the total cable weight as the cables covered in Figure 10. However, for the larger temperature ranges the cable movement is somewhat less, and this characteristic tends to counterbalance the relatively smaller life to cracking in manholes for this cable as compared to belted type cable for a given movement at a duct mouth. Further field measurements are to be made. Other figures similar to Figure 11 could be plotted to show the data, for example, for field measurements on 350,000-circular-mil three-conductor 13-kv and 35-kv belted cables (cables 6 and 7, Table IV). These two cables have lower cross-section-to-weight ratios; and the field measurements at temperature ranges of about 10 or 20 or 30 or 42 degrees centigrade brought out that the movements were actually lower than on Figure 10. Similarly, cables 8, 10, and 11 of Table IV, that is, single-conductor 69-kv cables of the ordinary type, have lower cross-section-to-weight ratios than the cables of Figure 10, and their measured movement averages somewhat less. Figure 10 is used for predicting the probable movement of cables such as cables 6, 7, 8, 10 and 11 because of convenience and the assurance that the movement values will be conservatively high.

A direct relationship between cable movement and sheath life in service is not usually determinable except in special cases. As an example of the latter, the 69-kv cable in Chicago may be cited. There are over 4,000 lengths of 750,000- and 1,000,000-circular-mil single-conductor solid-type 69-kv cable on the system. Practically all of it has been in service about 14 years, and no sheath cracks due to movement have occurred. The daily temperature variation has been about 8 or 10 degrees centigrade throughout the life of the cables, and from Figure 10 the corresponding movement is found to be not more than about 0.15 inch at each duct. The manholes are all about $11^{1}/_{2}$ or 12 feet long, and the offset between the center of the cable at the duct mouth and center of joint is about 19 inches. On the basis of the usual distribution of measured cable movements for a given load, as shown on Figure 10, it has been computed that the average life of a cable line should be at least three times the period between installation and the occurrence of the first cracks. From this it may be concluded that the average life of the sheath on these cables will be at least 45 years. From this it is reasonable to conclude at once that any cable, excepting three-conductor shielded cable, having the same over-all diameter (2.8 inches—see Table IV) and ratio of copper cross section to total weight and installed in 12-foot manholes and subjected to not more than ten degrees centigrade daily temperature variation would have a life of at least 45 years.

If the expected daily temperature variations were to be larger, say 20 degrees centigrade, the probable life could be estimated as follows: From Figure 10 the daily movement would be about 0.37 inch. From Figure 8 the life under test conditions at 0.37-inch movement is roughly one third the life at 0.15 inch. From the estimates at the end of the previous paragraph, divided by three, it appears that the sheath life in service with a 20-degree range would be, perhaps, 15 years.

The big and outstanding point is that small increases in loading are reflected through the square law in sizable increases in temperature range; and these, in turn, cause large cable movements, because the cable movement increases faster than the first power of the temperature range. Furthermore, the sheath life decreases much faster than the first power of the increase in movement.

It has been found next to impossible to determine directly the numerical relation between cable movement and the actual sheath life of three-conductor cable in service. A rather unsatisfactory alternative which was perforce adopted, might be used by any utility in studying its own operating records for a given type of cable. This method results in data such as shown in Figures 13 and 14 of the previous paper. Such data may be extrapolated for higher loads, using a relation such as in Figure 8.

Referring to Figure 8, detailed studies of over 300 sheath cracks and history in service of the cables involved indicate that the number of daily cycles to cracking for cable in service is less than the number of cycles to cracking in the dummy manhole for a given movement. The ratio may be roughly 50 per cent. The duration of each dummy manhole cycle is only 70 seconds as compared with 24 hours for cable in service. The longer time for one thing results in relatively more plastic flow of the sheath. Some field results indicate that vibration or special conditions in the manhole may in some cases lower the life in service considerably below the usual value. Another factor tending to decrease the sheath life in manholes as compared to life in test is that the sheaths in manholes on well-loaded lines are at higher temperatures than sheaths in the laboratory; this influence would become larger with wartime loading of cables at increased temperatures.

Table VI. Data on Movement of Long Lengths of Single-Conductor Cable in Cincinnati

Actual Cable Length—Feet	Total Movement for 38 C Temperature Variation—Inches	Calculated Length of Cable to Produce Same Movement as Measured—Feet
750	2.0	320
840	2.50	420
995	2.375	400
529	2.625	470
598	1.875	290
812	2.25	360
874	3.375	640
		Average 410

In estimating the effects of the higher loads due to the present emergency, it should be kept in mind that the cable has been in service for some time, and possibly one third or more of the sheath life, for example, has been spent. This should mean, for example, that with new daily loading, which would for a new cable give say ten years' life, one may obtain only five or seven more years' life for the cable in service, and a relatively high rate of sheath cracking may occur within a year or two.

On the other hand, the effect of emergency loads that may be carried for a day or two once a year is practically negligible, assuming movements up to about two inches. Some of the tests in the dummy manhole disclose no material effect on the life of the sheath when the movement was changed from the regular test value of 0.75 inch to 2.00 inches for one cycle every 170 cycles. The calculations based on data such as in Figure 8 indicate that the equivalent movement during emergencies should have no large effect on the sheath life. Of course, incidental to such a movement at the duct mouth, there should be room for the cable to move freely in the manhole without damage to the accessories or buckling of the cable.

Mitigation of Sheath-Crack Troubles

One result is certain to come from the heavier loads to be carried by existing cables in these days: sheath cracks will be much more numerous than in the past. The use of cable for repairs is going to be accelerated, unless means can be found to

(*a*). Lengthen the life of present sheaths.

(*b*). Repair them without the use of replacement cable, or both.

A direct line of attack now being tried in Chicago is to stop the cable from moving at the duct mouth. Specially designed grips of the woven basket type were laced to the cable at each duct mouth and anchored there with an adjustable tensioning device. The tension was brought up till the movement had been reduced to a negligible value. Incidentally, prior to making this installation, the force required to restrain the daily movement of three-fourth inch at the duct mouth was actually measured by dynamometer at the location of the test and was found to be around 1,000 to 1,500 pounds. After one year's trial no damage to the sheath because of the grips has been detected. One trouble is that the cable cools during the winter, and the grip is loosened, with the result that the cable can move back and forth at the duct mouth during the cooler part of the year unless extra trips are made to adjust the tension. The trial is being continued.

Another possible line of attack is to reduce the maximum strain in the lead. The strains have been measured at a few locations on cables in service and agree pretty well with the strains found in laboratory tests. The maximum strain occurs adjacent to the joint wipes for normal cable training with the joints mounted to slide freely toward the manhole wall and back. If the joint is blocked so that it cannot slide, two things seem to happen. The cable movement is reduced because of the increased resistance to it in the manhole, and the strain is distributed more uniformly throughout the bends in the cable, thus reducing the maximum strain. A discussion of this latter effect is given in the previous paper. In two dummy manhole tests, the average sheath life with the joint blocked was about 20 per cent more than with normal installation. The movement was three-fourths inch at each duct in both cases. In some field trials the joints were blocked in several manholes, and the movement decreased about 30 per cent. This suggests that the two factors together would double the life of the sheath, and so blocking of the joints should certainly be investigated further. One point not yet solved is the means for successfully taking care of effects of seasonal variations of conduit and cable temperatures.

A different line of attack is to develop some satisfactory method of repairing cracked sheaths. Experience in Chicago has been that solder patches and tight sleeves are quite unsatisfactory, mainly because, by the time a sheath crack is found in service, the entire length of sheath in the manhole is nearly worn out. After being patched, it cracks somewhere else in a year or two. From experimental work originated by W. B. Elmer, formerly of Boston Edison Company, and taken up in Chicago, it has been found that if a short section of sheath in each training bend is removed and replaced by a flexible insert, such as a Sylphon bellows, the strain on the remainder of the sheath

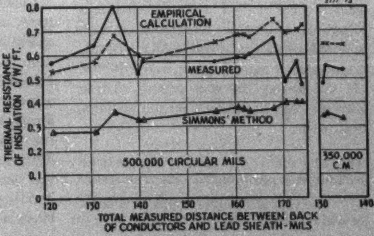


Figure 12. Thermal resistance of insulation of three-conductor sector-shielded cables

becomes almost negligible. In one Chicago test a cable was run to failure with the joint blocked. The life was 8,417 cycles. At the end of the test, four six-inch Sylphons were installed in the bends near the joint and near the duct mouths, with the lead sheath removed under the Sylphons. The remaining sheath was clearly fatigued almost to failure. The cable was tested for an additional 16,805 cycles without any failure occurring. The deformations were measured and found to be concentrated in the Sylphons, being negligible in the remaining lead sheath. The question of mechanical problems and corrosion problems with the Sylphons, especially the latter problem, has not been completely solved so far as long life is concerned, but seems adequately solved for use at least during an emergency period of a couple of years.

One disadvantage of Sylphon repairs is that the line has to be taken out of service, the joint disassembled to install the Sylphons, and then rebuilt. The ideal repair from the standpoint of economy and speed would be to make a flexible insert out of some sort of hand-wrapped material in tape or strip form. Several of the most promising materials selected from the synthetic rubbers and plastics have been tried, with unsatisfactory results. The best results so far have been obtained with Glyptal 1,782 tape applied with Glyptal 1,276 cement. When tested on cable under water with pressure and vacuum cycles applied to the cable and moisture detectors under the hand-applied tape, lives of 28,000 cycles and over 43,000 cycles were obtained on two samples. The severity of manhole conditions, however, would substantially shorten the life of the material in actual service. Something better is desired, but it seems that Glyptal could probably be used if necessary with fairly satisfactory results.

Allowable Operating Temperatures

As a result of the studies discussed in parts I and II, the conditions for various types of cable in Chicago were reconsidered early in 1942, and new maximum allowable conductor temperatures were adopted as shown in Table VII. In addition special higher values were adopted for use during the war period. These wartime values are based on the assumptions that:

(*a*). The life of the cable system will be used up at a much higher rate than usual.

(*b*). The rate of troubles in cables in service will increase.

Table VII. Maximum Allowable Operating Temperatures for Impregnated-Paper-Insulated Lead-Covered Cables Adopted for Use in Chicago in 1942

Operating Voltage—Volts	Type of Cable	Maximum Allowable Conductor Temperature—Degrees Centigrade: Normal Operation	Emergency Operation
120–240	All	90 (100)	115 (135)
4,000	All	85 (95)	105 (120)
12,000	Belted made prior to 1931	78 (85)	90 (100)
12,000	Belted made in 1931 and later	78 (85)	95 (105)
12,000	Single-conductor and shielded three-conductor	78 (85)	105 (120)
33,000	Shielded, three-conductor cable with joints filled with "solid" compounds	71 (77)	84 (90)
66,000	Single-conductor, solid-type cable with joints filled with "solid" compounds	65 (68)	70 (75)
66,000	Oil-filled; single-conductor and three-conductor	78 (85)	105 (115)
132,000	Oil-filled; single-conductor	78 (85)	100 (110)

The values in parentheses are for use under wartime conditions only.

These temperatures for Chicago conditions are given only as an example of what one company is doing and are not considered to be necessarily applicable for other systems in which the installation and operating conditions are different. In general, the temperatures for normal operation of the higher-voltage cables were limited by the danger of excessive sheath cracking in manholes, while the temperatures for emergency operation of all cables and for normal operation of low-voltage cables were limited by danger of deterioration of the insulation or of excessive radial expansion of sheaths.

III. Heating Characteristics of Cables and Conduits

Three-Conductor Shielded Cable

Incidental to the accelerated aging tests on three-conductor shielded-type cable, mentioned in Part I, the thermal drop between the conductors and sheath was determined. The method was to average the results from at least four tests for each sample, the tests being made at the various temperatures from 60 to 115 degrees centigrade. The thermal drop was determined by the usual method of dividing the temperature difference between the conductor and sheath by the sum of the losses in the conductor and half the dielectric loss in the insulation.

Calculated values of thermal resistance were determined from the geometric dimensions of the cable using a thermal resistivity of 600 degrees centigrade per watt per centimeter cube. The values calculated by the D. M. Simmons[3] method are appreciably below the measured values, as indicated in Figure 12.

An empirical method of calculation was developed. It is based essentially on heat flow radially from only the backs of the sector conductors to the sheath. This method uses the usual formulas for one single-conductor cable with an empirical multiplier as follows:

$$R_i = \frac{\pi D_A}{\pi D_A - K} 0.00522\rho \log_e \frac{D_s}{D_i} \qquad (1)$$

R_i = the thermal resistance of insulation in ohms per foot

ρ = the thermal resistivity of insulation

D_0 = the inside diameter of the sheath

D_i = the diameter over the copper of the cabled conductors

D_A = the average of D_0 and D_i

K equals six times the insulation thickness for conductors with corners of small radius, such as 500,000-circular-mil "compack" conductors. For conductors with more rounded corners, *K* has been found to be greater up to about nine times the insulation thickness.

As indicated in Figure 12, this method gives a fair agreement with the measured results. The general idea is that the thermal resistance of air gaps and of contacts between the various materials in series between the conductor and sheath all add to the thermal drop to more or less counterbalance the low resistivity of the metal shielding tapes and binder tapes. The results in the figure apply to cables having a net measured insulation ranging from 89 to 140 mils. The total measured distance between conductor and sheath, however, was some 20 to 40 mils larger.

Apparently for three-conductor shielded cables with relatively small insulation, the effect of the regular three-mil copper shielding tape on carrying heat out from the inner portion of the cable is small. This point was checked further by tests on one sample of cable with wires in the center filler space. The results of measurements of resistance of these wires generally indicate that the rise in temperature of the wires above the sheath temperature was about ten per cent less than the rise of the temperature of the conductors above sheath temperature.

For thicker insulations, *K* in the above formula would probably be less than indicated; that is, the copper shielding tape would be of relatively more benefit in keeping down the temperature rise.

Thermal Resistivity of the Sheath

During the aging tests of the three-conductor cables in the still air in the laboratory, sheath temperatures were determined. From these data the surface thermal resistivities were determined, and results are shown for four typical cables in Figure 13.

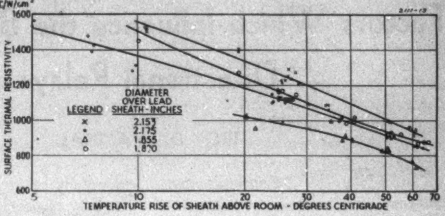


Figure 13. Surface thermal resistivity of lead cable sheaths (by tests in still air in laboratory)

1. One striking point is that for temperature rises of the sheath of 20 degrees or less, a cable of about two-inch diameter might have a surface thermal resistivity above the usual assumed value of 1,200.

2. There is considerable variation between the sheaths. The tests shown indicate a range of about 25 per cent for some cables of practically the same diameter.

3. The resistivity drops with temperature rise, as may be expected, the decrease between a 10-degree rise and a 40-degree rise being about 30 per cent.

Increasing Carrying Capacity of Conduits

In making a new design of conduit, it may be found from the standpoint of the desired loading of the cable that the originally proposed design of conduit would be inadequate. This situation may be remedied, for example, by spreading the ducts or by increasing the number of ducts and spreading out the cables by placing them in some of the outside ducts. Sometimes the problem arises as to whether for a given number of ducts the amount of heat that may be generated in them may be increased better

(*a*). By placing soil of low thermal resistance around the conduit instead of the soil found in the street.

(*b*). By simply having the conduit installed at a greater depth below the street surface.

This problem arises particularly where conduits are to be installed in soil consisting largely of slag or fine sand or miscellaneous backfill in the first two, four, or six feet of soil below the street level.

In the first method the special backfill material of low thermal resistivity is placed for a space of two feet on each side of the conduit and above the conduit for the distance from the street surface down to the bottom of the conduit. In such case the distance from the top of the conduit to the street surface may be the usual figure of about 2¹/₂ feet. In the other method the cover over the conduit may become several feet, and the distance from the street surface to the bottom of the conduit may become, say, nine feet. There are two distinct technical advantages of this second method:

1. The conduit is brought closer to the ground water level.

2. The maximum temperature in the summer will be less for the base earth temperature than for conduit installed at the usual depth.

Sometimes soil conditions are such that even the replacement of so-called poor soil with a good soil will not solve the problem, because relatively little water is drawn into the so-called good soil to produce a low thermal resistance and good thermal dissipating characteristics for the conduit. The advantage of the lower value for maximum earth temperature for summer conditions is of particular interest, because the rating of the cable limits the loading more frequently in the summertime than in the wintertime.

One set of calculations on the economics involved showed that for installations of either a 6-duct conduit or a 12-duct conduit, the cost per kilovolt-amperes of allowable carrying capacity was appreciably less for the second scheme as compared with scheme (*a*). (Replacement of poor soil with soil of good thermal conductivity has been found very helpful in some cases for existing conduits.)

The method to be used for a new installation, however, depends to a large extent upon the conditions prevailing in the particular case. The gain obtained from increasing the depth of the conduit would be somewhat counterbalanced by the increase in required depth of the manholes and extra troubles that would accrue from water encountered in the manholes. Also, in some localities, rock is hit at relatively small depths.

Existing installations of other utilities may be of such a nature as to have a bearing on the method to be used. For installations in approaches to bridges, soil of poor conductivity may be very deep so that method (*b*) is not applicable. In this case method (*a*), with or without spreading out the cables by special duct formations, is effective. If further increase in current-carrying capacity of cables is desired for such locations, larger sizes of cables than are normally used along the remainder of the lines may be installed, thereby decreasing the losses in the conduit. For conduits under construction where poor soil is encountered in spots along a relatively small portion of the distance between manholes, replacement of soil has been found advisable.

Temperatures of Conduit

In the past usually most of the cables in a given conduit operated with daily maximum loads below the normal ratings. This phenomenon is recognized in establishing the ratings. If it were known that the several cables in a conduit were to operate part of each day throughout their lives at full normal rating, then the calculated normal ratings of each design of cable would be less than are usually established.

With wartime conditions, the maximum daily loading of cables usually increases. Also, the daily load factor usually increases. This means a substantial increase in the average amount of heat generated over each 24-hour period and a corresponding increase in conduit temperatures, even though *no* increase in normal ratings of the cables may have been established. The resulting copper temperatures may become much greater than occur in peacetimes, and even exceed the temperatures given in standards by several degrees or more.

In the previous paper an allowable limit of about 50 degrees centigrade was mentioned for the empty ducts of conduits in order to avoid

(*a*). Excessive drying out of the conduit and soil.

(*b*). An increase in heating constant.

(*c*). Resulting excessive conduit and cable temperatures.

Further field data have been studied. An analysis of heating characteristics of several conduits installed in soil of typical moisture content did not indicate an in-

Factors Which Influence the Behavior of Directional Relays

TROY D. GRAYBEAL
ASSOCIATE AIEE

Synopsis: Very little material dealing with the factors (such as network dissymmetries) which lead to incorrect operation of directional relays has appeared in the technical literature. Practical experience has been the guide as to the relative merits of the 12 possible connections which utilize either line-to-neutral or line-to-line voltages and line currents or the differences of line currents to actuate the relay elements. In this article a graphical method for analyzing directional-relay operation is developed, utilizing circle diagrams and symmetrical components, and the method is applied to general cases to ascertain the effects of network dissymmetries.

Since the relay volt-ampere expressions in terms of symmetrical components are too complicated to show the relative merits of the 12 connections, the expressions also are given in terms of the modified symmetrical components introduced by Edith Clarke and now widely used in network-analyzer studies of unbalanced faults. Circle diagrams based upon these modified components show conclusively that the ordinary 90-degree, 30-degree, and 60-degree connections are more free from disturbing influences than the other nine and therefore give more reliable operation in most practical applications of directional relays. The particular connection which will be most reliable at any given location can be determined by the methods given here when the constants of the power network are known.

THE demand for reliable and continuous service from modern power systems has made it essential that short circuits and other faults cause as little disturbance as possible to the system as a whole. Ideal protection would consist of relays which would locate accurately the faulted equipment and cause only such equipment to be disconnected from the system when short circuits occur. Since overcurrent and distance relay elements as used in loop and parallel feeder networks cannot differentiate between faults on the incoming and outgoing sides of a breaker, directional elements or relays must be used for this purpose. To prevent unnecessary loss of load and undamaged apparatus, and to prevent undue damage to the faulted equipment, the directional relays must be designed and adjusted to operate correctly for as many conditions of operation as possible on all types of faults, and in particular for those conditions which are of most frequent occurrence. A comprehensive knowledge of the behavior of directional relays of a system is necessary in order that this result may be achieved.

Of the many types of directional relays which are manufactured, this paper is limited to those which operate on the wattmeter principle. Other types in which voltage restraint coils are used are closely related, from the standpoint of operating principle, to distance relays. These have been treated in detail by J. H. Neher[1] who has developed excellent methods for analyzing the performance of different forms of the distance relay.

The behavior of a wattmeter-type directional relay depends upon the vector volt-amperes supplied to the relay element. The magnitude and angle of this quantity is obtained from the product, in the vector sense, of the current and the conjugate of the voltage at the relay. The torque developed by the relay element is proportional to $|VI|\cos(\theta+\phi)$. The angle between the relay voltage and current is θ, and ϕ is the "relay angle," which is the angle between the current and voltage applied to the relay for which given magnitudes of these quantities cause maximum torque to be developed. The relay angle is positive if the current leads the voltage for this condition.

The vector volt-ampere product may be expressed in terms of symmetrical components. The different terms of the expression then are evaluated separately and combined to indicate the direction of the relay torque. The coefficients of the several volt-ampere terms are given in Table I for both single-phase and poly-

Paper 42-90, recommended by the AIEE committee on protective devices for presentation at the AIEE summer convention, Chicago, Ill., June 22–26, 1942. Manuscript submitted July 11, 1941; made available for printing April 20, 1942.

TROY D. GRAYBEAL is instructor in electrical engineering, University of California, Berkeley, Calif.

The author acknowledges his indebtedness to J. C. Balsbaugh of Massachusetts Institute of Technology, Robert L. Frank of the Bulldog Electric Products Company, and Paul Morton of the University of California, for important material and suggestions.

---

crease in heating constant for various temperatures up to 50 degrees.

A longitudinal duct temperature survey made in June 1941 along a conduit section showed temperatures along the portion of the length not affected by the presence of the manholes of 39 to 61 degrees centigrade. This conduit is located in miscellaneous fill consisting chiefly of cinders. This fill, being of a porous nature and well drained, normally holds very little moisture. The heating constant was found to be poor, but an analysis indicated no change from the previous year when the duct temperatures were much lower because of lighter loads on the cables. Other data, including experiences of over 20 years ago in Chicago, were also considered.

All of the data indicate that in many cases the allowable duct temperature limit of 50 degrees is too low, and at least for wartime conditions the maximum temperature for normal loading may be 55 or 60 degrees centigrade and five degrees higher for emergency loading.

Some factor of safety should be allowed for temperature variation between manholes, if duct temperatures are measured by the usual method at one spot along the conduit section. For the case where the idle duct temperatures were 39 to 61 degrees in the conduit section, the temperature 20 feet from the duct mouth was only 47 degrees, or 14 degrees lower than the maximum value. Such variation can be expected in conduit runs installed in soil of poor thermal conductivity when operating at temperatures of 50 degrees or more. Even in soil having good thermal characteristics, some variation, such as 15 or 25 per cent, in heating constant between two manholes may be expected, according to many field measurements. In this case, if the rise of the conduit temperature above base earth conditions is like a typical case in peacetime, that is, 16 degrees, then the variation is only four degrees; but, if the cable loading is up and the average conduit-temperature rise is 32 degrees, then the variation in duct temperatures may become about eight degrees—a large amount.

References

1. LOAD RATINGS OF CABLE, Herman Halperin. AIEE TRANSACTIONS, volume 58, 1939, October section, pages 535–53.

2. RATE OF DETERIORATION OF IMPREGNATED WOOD-PULP CABLE PAPER SUBJECTED TO HEAT ONLY. National Electric Light Association *Proceedings*, volume 86, 1929, pages 1493–1504.

3. CALCULATIONS OF ELECTRICAL PROBLEMS OF UNDERGROUND CABLES, D. M. Simmons. *The Electric Journal*, May–November 1932.

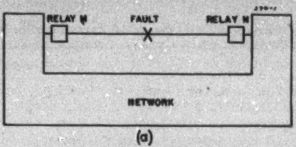
phase relays for all 12 possible connections which utilize either wye or delta voltages and either wye or delta currents to actuate the relay elements. "Line-to-neutral" voltages are referred to as *wye* voltages and "line-to-line" voltages as *delta* voltages. Likewise, "line" currents are *wye* currents and the "differences of line currents" are *delta* currents.

In order to study the effects of network dissymmetries, it is necessary to consider special network conditions beginning with the simpler cases. The general network representation illustrated in Figure 1 is used. In most cases, the impedance $\mathbf{Z}_Z$ is considered zero, since it has no effect whatever upon the distribution of the total fault current between sides *M* and *N*. The effect of $\mathbf{Z}_Z$ is to modify the voltage $\mathbf{V}_M$ which appears at the relay. Since the important consideration is the phase of $\mathbf{V}_M$ with respect to the relay current, $\mathbf{Z}_Z$ has no effect unless its angle is different from that of the impedance $\mathbf{Z}_M$. In most actual power networks these angles will not differ greatly.

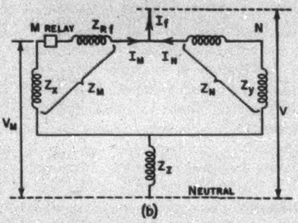
The simplest possible case is represented by $\mathbf{Z}_Z=\mathbf{Z}_{Rf}=0$, $\mathbf{Z}_1=\mathbf{Z}_2=\mathbf{Z}_0$, and $\mathbf{Z}_M=\mathbf{Z}_N$ in all three sequence networks. When these conditions are substituted into the general relay volt-ampere expressions of Table I, four of the connections show inherent tendencies toward incorrect relay operation. If a particular connection will not give satisfactory performance in such a simplified system, it is of little use in a large complicated power network. Such connections, therefore, are not considered further and are omitted from Tables II and III.

Determination of Relay Behavior By Means of Circle Diagrams

Circle diagrams make possible visualization of the relations which must exist between the various sequence quantities as fault resistance or network constants vary. Thus, in Figure 3 drawn for the line-to-ground fault in terms of symmetrical components, the positive-sequence voltage vector is constrained to fall within the semicircle CEE_{f0} for any value of fault or network resistance; with a purely reactive network the vector falls on the circumference. Similarly, the sum of the negative- and zero-sequence voltages must always fall within the semicircle OAC, and the current within the semicircle OI_fF. The ranges of possible variation of the sequence voltages and currents are shown by the diagram to be confined to definite areas which are determined by the network reactances. This situation would be very difficult to visualize from the symmetrical-component equations alone.



(a). Single-line drawing of an actual power network



(b). Equivalent circuit which represents either the positive-, negative-, or zero-sequence network of the actual system

Figure 1

The nature of the circle diagrams may be recognized from the circuits of Figure 2 in which the fault resistance is the variable parameter. These are series circuits in which $\mathbf{E}_{f0}$ (the positive-sequence voltage at the point of fault before the occurrence of the fault) is the driving voltage. In any circuit of this type, the current vector, as well as the vector voltage across any constant impedance portion of the circuit, describes a circle as the resistance varies.

For a line-to-ground fault, the voltage $\mathbf{E}_{f0}$ is laid off horizontally to scale and divided at point C (Figure 3) according to the ratio $CE_{f0}/OC=X_1/(X_2+X_0)$. If the magnitude of the fault current is known or assumed, the vector OI_f is constructed to scale so as to form a chord to the semicircular current locus. Then the lines OA perpendicular to $\mathbf{I}_f$, AE parallel to $\mathbf{I}_f$, and EE_{f0} perpendicular to $\mathbf{I}_f$ are drawn. The vector OB, which represents the voltage drop across both the negative- and zero-sequence networks, is constructed at an angle with $\mathbf{I}_f$ equal to the angle of the negative- and zero-sequence network impedances combined in series. The line DE_{f0} is drawn to intersect the line AE at the angle of the positive-sequence impedance to locate the end of the positive-sequence voltage vector $\mathbf{V}_1$. The vector BD represents the voltage drop $3\mathbf{I}_fR_f$ consumed by the fault resistance. The two voltage vectors $-\mathbf{V}_2$ and $-\mathbf{V}_0$ lead the total fault current by the angles θ_2 and θ_0 respectively, and the lengths are such as to complete the parallelogram $O(-V_2)B(-V_0)$. The division of current in either sequence network is such that $\mathbf{I}_M$ and $\mathbf{I}_N$ lag the corresponding sequence voltage drop by the angles of the corresponding sequence impedances $\mathbf{Z}_M$ and $\mathbf{Z}_N$ respectively. The location of all the circular loci in terms of the network impedances is given in detail in Figure 4, and the mathematical basis for the diagram in appendix A.

The two semicircles OAC and CEE_{f0} (Figure 3) are the loci of the two voltages $(-\mathbf{V}_2-\mathbf{V}_0)$ and $\mathbf{V}_1$ respectively, when the resistances of the sequence networks are neglected. When resistances are considered, the ends of the two corresponding voltage vectors must lie somewhere *inside* the two semicircles. It is impossible for either of the two voltages $-\mathbf{V}_2$, $-\mathbf{V}_0$ or their vector sum, when laid off from O, to fall above the semicircular arc OAC. Likewise, the positive-sequence voltage $\mathbf{V}_1$ cannot fall below the semicircular arc CEE_{f0} as long as the networks are inductive in nature.

The angles between the fault current $\mathbf{I}_f$ and the negative- and zero-sequence voltages are independent of the fault resistance. The angle between the positive-sequence voltage and the fault current varies with the fault resistance, but is restricted to the range zero to 90 degrees with the current lagging. If the impedance $\mathbf{Z}_M$ is more resistive than $\mathbf{Z}_N$, the current coming to the fault from side *M* will lead the total fault current, and the current coming from side *N* will lag the total fault current. The vector sum of these two components must, of course, make up the total. The current $\mathbf{I}_{2-N}$ coming from side *N* cannot lag the voltage $-\mathbf{V}_2$ by more than 90 degrees. It follows, therefore, that $\mathbf{I}_{2-N}$ can never lag $\mathbf{E}_{f0}$ by more than 90 degrees unless the zero-sequence network is more reactive than the negative-sequence network. In the same way, $\mathbf{I}_{0-M}$ can never lead the total fault current $\mathbf{I}_f$ by an angle greater than the angle of the zero-sequence network. As a limiting case, it may lag the voltage $-\mathbf{V}_0$ by as much as 90 degrees and thus lie in the third quadrant. If the currents $\mathbf{I}_M$ and $\mathbf{I}_N$ are equal in all three sequence networks, the maximum angle between them is given by the expression $(90°-\theta_2+\theta_0)$. These limitations as to the possible ranges of the sequence quantities are of value in the determination of the direction of relay torque from the relay volt-ampere expressions, particularly when the expressions are used to judge the relative merits of the various relay connections.

Impedance between the relay and point of fault affects principally the magnitude

Table I. Coefficients of All Terms in the Symmetrical-Component Expressions for Vector Volt-Ampere Input to a Relay

	Relay Connection	Relay	Current-Voltage Product	Volt-Ampere Terms*								
				$(\text{conj } V_1)I_1$	$(\text{conj } V_1)I_2$	$(\text{conj } V_1)I_0$	$(\text{conj } V_2)I_1$	$(\text{conj } V_2)I_2$	$(\text{conj } V_2)I_0$	$(\text{conj } V_0)I_1$	$(\text{conj } V_0)I_2$	$(\text{conj } V_0)I_0$
Wye-Voltage Wye-Current Connections	Zero-degree	a	$(\text{conj } V_a)I_a$	1∠0	1∠0	1∠0	1∠0	1∠0	1∠0	1∠0	1∠0	1∠0
		b	$(\text{conj } V_b)I_b$	1∠0	1∠240	1∠120	1∠120	1∠0	1∠240	1∠240	1∠120	1∠0
		c	$(\text{conj } V_c)I_c$	1∠0	1∠120	1∠240	1∠240	1∠0	1∠120	1∠120	1∠240	1∠0
		Polyphase		3∠0				3∠0				3∠0
	60-degree ordinary	a	$-(\text{conj } V_a)I_b$	1∠60	1∠300	1∠180	1∠60	1∠300	1∠180	1∠60	1∠300	1∠180
		b	$-(\text{conj } V_b)I_c$	1∠60	1∠180	1∠300	1∠180	1∠300	1∠60	1∠300	1∠60	1∠180
		c	$-(\text{conj } V_c)I_a$	1∠60	1∠60	1∠60	1∠300	1∠300	1∠300	1∠180	1∠180	1∠180
		Polyphase		3∠60				3∠300				3∠180
	60-degree alternate	a	$-(\text{conj } V_a)I_c$	1∠300	1∠60	1∠180	1∠300	1∠60	1∠180	1∠300	1∠60	1∠180
		b	$-(\text{conj } V_b)I_a$	1∠300	1∠300	1∠300	1∠60	1∠60	1∠60	1∠180	1∠180	1∠180
		c	$-(\text{conj } V_c)I_b$	1∠300	1∠180	1∠60	1∠180	1∠60	1∠300	1∠60	1∠300	1∠180
		Polyphase		3∠300				3∠60				3∠180
Delta-Voltage Wye-Current Connections	90-degree ordinary	a	$-(\text{conj } V_A)I_a$	√3∠90	√3∠90	√3∠90	√3∠270	√3∠270	√3∠270			
		b	$-(\text{conj } V_B)I_b$	√3∠90	√3∠330	√3∠210	√3∠30	√3∠270	√3∠150			
		c	$-(\text{conj } V_C)I_c$	√3∠90	√3∠210	√3∠330	√3∠150	√3∠270	√3∠30			
		Polyphase		3√3∠90				3√3∠270				
	30-degree ordinary	a	$(\text{conj } V_A)I_c$	√3∠30	√3∠150	√3∠270	√3∠210	√3∠330	√3∠90			
		b	$(\text{conj } V_B)I_a$	√3∠30	√3∠30	√3∠30	√3∠330	√3∠330	√3∠330			
		c	$(\text{conj } V_C)I_b$	√3∠30	√3∠270	√3∠150	√3∠90	√3∠330	√3∠210			
		Polyphase		3√3∠30				3√3∠330				
	30-degree alternate	a	$-(\text{conj } V_A)I_b$	√3∠330	√3∠210	√3∠90	√3∠150	√3∠30	√3∠270			
		b	$-(\text{conj } V_B)I_c$	√3∠330	√3∠90	√3∠210	√3∠270	√3∠30	√3∠150			
		c	$-(\text{conj } V_C)I_a$	√3∠330	√3∠330	√3∠330	√3∠30	√3∠30	√3∠30			
		Polyphase		3√3∠330				3√3∠30				
Wye-Voltage Delta-Current Connections	90-degree	a	$(\text{conj } V_a)I_A$	√3∠90	√3∠270		√3∠90	√3∠270		√3∠90	√3∠270	
		b	$(\text{conj } V_b)I_B$	√3∠90	√3∠150		√3∠210	√3∠270		√3∠330	√3∠30	
		c	$(\text{conj } V_c)I_C$	√3∠90	√3∠30		√3∠330	√3∠270		√3∠210	√3∠150	
		Polyphase		3√3∠90				3√3∠270				
	30-degree	a	$-(\text{conj } V_a)I_C$	√3∠30	√3∠330		√3∠30	√3∠330		√3∠30	√3∠330	
		b	$-(\text{conj } V_b)I_A$	√3∠30	√3∠210		√3∠150	√3∠330		√3∠270	√3∠90	
		c	$-(\text{conj } V_c)I_B$	√3∠30	√3∠90		√3∠270	√3∠330		√3∠150	√3∠210	
		Polyphase		3√3∠30				3√3∠330				
	30-degree alternate	a	$(\text{conj } V_a)I_B$	√3∠330	√3∠30		√3∠330	√3∠30		√3∠330	√3∠30	
		b	$(\text{conj } V_b)I_C$	√3∠330	√3∠270		√3∠90	√3∠30		√3∠210	√3∠150	
		c	$(\text{conj } V_c)I_A$	√3∠330	√3∠150		√3∠210	√3∠30		√3∠90	√3∠270	
		Polyphase		3√3∠330				3√3∠30				
Delta-Voltage Delta-Current Connections	Zero-degree	a	$(\text{conj } V_A)I_A$	3∠0	3∠180		3∠180	3∠0				
		b	$(\text{conj } V_B)I_B$	3∠0	3∠60		3∠300	3∠0				
		c	$(\text{conj } V_C)I_C$	3∠0	3∠300		3∠60	3∠0				
		Polyphase		9∠0				9∠0				
	60-degree	a	$-(\text{conj } V_A)I_B$	3∠60	3∠120		3∠240	3∠300				
		b	$-(\text{conj } V_B)I_C$	3∠60	3∠0		3∠0	3∠300				
		c	$-(\text{conj } V_C)I_A$	3∠60	3∠240		3∠120	3∠300				
		Polyphase		9∠60				9∠300				
	60-degree alternate	a	$-(\text{conj } V_A)I_C$	3∠300	3∠240		3∠120	3∠60				
		b	$-(\text{conj } V_B)I_A$	3∠300	3∠120		3∠240	3∠60				
		c	$-(\text{conj } V_C)I_B$	3∠300	3∠0		3∠0	3∠60				
		Polyphase		9∠300				9∠60				

* To find the symmetrical-component expression for the total vector volt-ampere input to any relay, take the sum of all the column headings, each multiplied by the corresponding coefficient taken from the row opposite the relay considered. The voltage and current components in the network at the point where the relay is located must be used in these expressions.

of the voltages at the relay. As the impedance Z_{Rf} becomes larger and larger as compared with Z_M and Z_N, the negative- and zero-sequence voltages at *M* get smaller and smaller in magnitude. They are modified in phase only when the angle of Z_{Rf} is different from the angle of Z_M. In general, these angles will not differ greatly, and the predominate effect of impedance between the relay and fault point is to decrease the negative- and zero-sequence voltages and to increase the positive-sequence voltage. In an extreme case where all the resistance of side *M* is between the relay and fault point, and all the reactance beyond the relay, the negative-sequence voltage $-V_{2-M}$ would lie on a semicircle with $-V_2$ as the diameter. The relay voltage $-V_{2-M}$, therefore, may lie somewhat outside the semicircle *OAC*, but only in rare cases. The relative importance of Z_{Rf} is determined by how far back electrically from the fault point the relay is located. It becomes more important as the ratio Z_{Rf}/Z_M increases. This ratio is larger for a line connected into a large power-system network than for a line of the same constants connected into a small power-system network.

The diagram of Figure 3, together with others drawn for different network conditions, shows it is impossible to design a directional-relay scheme which will operate correctly for all possible faults on a normal power system. Erratic operation may be expected, if the angles of the impedances measured opposite ways from the fault point are greatly different, or if the angle of the impedance between the relay and point of fault is considerably different from that of the total impedance Z_M. In general, the relay on the more reactive side of the fault is less liable to operate incorrectly.

When load currents are present, they must be added vectorially to the fault components in the positive-sequence network. Load currents increase one of the components I_{1-M} or I_{1-N} and decrease the other. In cases where the load current is comparable in magnitude with the fault current, the total positive-sequence current may be modified in phase sufficiently to cause incorrect relay operation. The relay most subject to this effect is the one at the receiving end of the line, since at this point the two components of positive-sequence current flow in opposite directions.

In order to obviate the effects of load current, some form of overcurrent device must be used in conjunction with the relay to prevent operation unless overcurrent or fault current exists in the system. The current balance scheme, which is commonly used for the protection of paralleled feeders, makes use of two current transformers, one in each feeder, with secondary windings connected so that the relay receives the difference of the two secondary currents. In this case, the relay current is given by the expression $I_f(1-f)$ which is independent of normal load current. I_f is the total fault current and f is the fractional length of the feeder from the relay to the point of fault. This scheme, however, is applicable only to feeders or other apparatus operating in parallel. In other cases where the load component of current may be comparable in magnitude with the fault components, the only workable scheme is the use of "ground relays" which operate on zero- or negative-sequence quantities only.

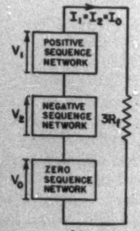
The ease with which directional-relay operation may be determined by use of the circle diagram is exemplified by Figure 5 which shows the behavior of an ordinary 90-degree-connected polyphase relay on a line-to-ground fault. The circle diagram is constructed from the network impedances in the manner outlined previously to give the magnitude and phase of the sequence voltages and currents at the relay. From Table I, the volt-ampere expression to be evaluated is $[(\text{conj } V_1)I_1\underline{|90}+(\text{conj } V_2)I_2\underline{|270}]$. The factor $3\sqrt{3}$ is omitted since the actual magnitude of the relay torque is unimportant. If the load current is small compared with the fault currents, I_1 is equal to I_2, and the expression may be written $(\text{conj } V_1-\text{conj } V_2)I_1\underline{|90}$. The relay torque then is proportional to the sum of the projections of the two voltages on the current vector I_1 rotated through the angle $(90°-\phi)$. Since both of these projections are positive for the conditions depicted in Figure 5, correct relay operation is indicated.

An alternative method which may be used to advantage when the volt-ampere expression consists of only two terms, makes use of the position of the current vector where the two voltage projections on the current are equal in magnitude but opposite in sign. A line in this position is designated an "equiprojection" line, and represents that phase position of the current for which the torque developed by an element having a relay angle of zero degrees is zero. If the relay angle ϕ is not zero, the zero-torque line is ϕ degrees leading the equiprojection line.

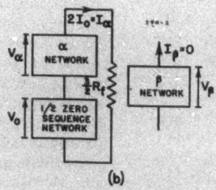
The equiprojection line is located by dropping a perpendicular from 0 to the line joining the ends of the two voltages $V_1\underline{|90}$ and $(-V_2\underline{|-90})$. Since the relay operates correctly if the current lags the zero-torque line by less than 180 degrees, correct operation is indicated in Figure 5, where the current is near the position for maximum torque for the two voltages concerned.

For the ordinary 90-degree-connected polyphase relay, the equiprojection line will coincide with E_{f0} if the positive- and negative-sequence networks are identical. Thus, disregarding the secondary effects of load currents, the direction of the relay torque is independent of the fault resistance for this connection.

When the volt-ampere expressions consist of more than two terms, or when the two terms do not involve the same current, the first method described must be used. An example of this character is given in detail in appendix B.

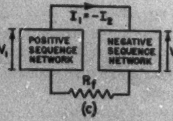


(a). Line-to-ground fault in terms of symmetrical components

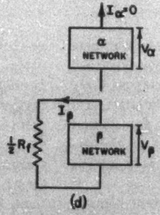


(b) Line-to-ground fault in terms of modified symmetrical components

Figure 2 (left). Sequence network connections representing line-to-ground and line-to-line faults



(c). Line-to-line fault in terms of symmetrical components



(d). Line-to-line fault in terms of modified symmetrical components

Polyphase Directional Relays

A three-phase relay is less subject to erratic operation than are the three single-phase relays otherwise used on a three-phase system, since an incorrect operation of any one relay will result in an incorrect switching operation. That the three single-phase relays will behave dif-

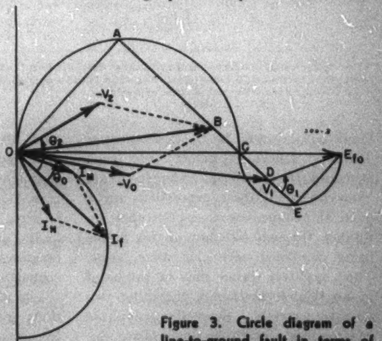


Figure 3. Circle diagram of a line-to-ground fault in terms of symmetrical components

Table II. Coefficients of All Terms in the Modified Symmetrical-Component Expressions for Vector Volt-Ampere Input to a Relay

	Relay Connection	Relay	Current-Voltage Product	Volt-Ampere Terms*								
				$(\text{conj } V_\alpha)I_\alpha$	$(\text{conj } V_\alpha)I_\beta$	$(\text{conj } V_\alpha)I_0$	$(\text{conj } V_\beta)I_\alpha$	$(\text{conj } V_\beta)I_\beta$	$(\text{conj } V_\beta)I_0$	$(\text{conj } V_0)I_\alpha$	$(\text{conj } V_0)I_\beta$	$(\text{conj } V_0)I_0$
Wye-Voltage Wye-Current Connections	60-degree ordinary	a	$-(\text{conj } V_a)I_b$	$\frac{1}{2}$	$-\frac{\sqrt{3}}{2}$	-1	0	0	0	$\frac{1}{2}$	$-\frac{\sqrt{3}}{2}$	-1
		b	$-(\text{conj } V_b)I_c$	$-\frac{1}{4}$	$-\frac{\sqrt{3}}{4}$	$\frac{1}{2}$	$\frac{\sqrt{3}}{4}$	$\frac{3}{4}$	$-\frac{\sqrt{3}}{2}$	$\frac{1}{2}$	$\frac{\sqrt{3}}{2}$	-1
		c	$-(\text{conj } V_c)I_a$	$\frac{1}{2}$	0	$\frac{1}{2}$	$\frac{\sqrt{3}}{2}$	0	$\frac{\sqrt{3}}{2}$	-1	0	-1
		Polyphase		$\frac{3}{4}$	$-\frac{3\sqrt{3}}{4}$	0	$\frac{3\sqrt{3}}{4}$	$\frac{3}{4}$	0	0	0	-3
	60-degree alternate	a	$-(\text{conj } V_a)I_c$	$\frac{1}{2}$	$\frac{\sqrt{3}}{2}$	-1	0	0	0	$\frac{1}{2}$	$\frac{\sqrt{3}}{2}$	-1
		b	$-(\text{conj } V_b)I_a$	$\frac{1}{2}$	0	$\frac{1}{2}$	$-\frac{\sqrt{3}}{2}$	0	$-\frac{\sqrt{3}}{2}$	-1	0	-1
		c	$-(\text{conj } V_c)I_b$	$-\frac{1}{4}$	$\frac{\sqrt{3}}{4}$	$\frac{1}{2}$	$-\frac{\sqrt{3}}{4}$	$\frac{3}{4}$	$\frac{\sqrt{3}}{2}$	$\frac{1}{2}$	$-\frac{\sqrt{3}}{2}$	-1
		Polyphase		$\frac{3}{4}$	$\frac{3\sqrt{3}}{4}$	0	$-\frac{3\sqrt{3}}{4}$	$\frac{3}{4}$	0	0	0	-3
Delta-Voltage Wye-Current Connections	90-degree ordinary	a	$-(\text{conj } V_A)I_a$	0	0	0	$\sqrt{3}$	0	$\sqrt{3}$			
		b	$-(\text{conj } V_B)I_b$	$\frac{3}{4}$	$-\frac{3\sqrt{3}}{4}$	$-\frac{3}{2}$	$\frac{\sqrt{3}}{4}$	$-\frac{3}{4}$	$-\frac{\sqrt{3}}{2}$			
		c	$-(\text{conj } V_C)I_c$	$-\frac{3}{4}$	$-\frac{3\sqrt{3}}{4}$	$\frac{3}{2}$	$\frac{\sqrt{3}}{4}$	$\frac{3}{4}$	$-\frac{\sqrt{3}}{2}$			
		Polyphase		0	$-\frac{3\sqrt{3}}{2}$	0	$\frac{3\sqrt{3}}{2}$	0	0			
	30-degree ordinary	a	$(\text{conj } V_A)I_c$	0	0	0	$\frac{\sqrt{3}}{2}$	$\frac{3}{2}$	$-\sqrt{3}$			
		b	$(\text{conj } V_B)I_a$	$\frac{3}{2}$	0	$\frac{3}{2}$	$\frac{\sqrt{3}}{2}$	0	$\frac{\sqrt{3}}{2}$			
		c	$(\text{conj } V_C)I_b$	$\frac{3}{4}$	$-\frac{3\sqrt{3}}{4}$	$-\frac{3}{2}$	$-\frac{\sqrt{3}}{4}$	$\frac{3}{4}$	$\frac{\sqrt{3}}{2}$			
		Polyphase		$\frac{9}{4}$	$-\frac{3\sqrt{3}}{4}$	0	$\frac{3\sqrt{3}}{4}$	$\frac{9}{4}$	0			
	30-degree alternate	a	$-(\text{conj } V_A)I_b$	0	0	0	$-\frac{\sqrt{3}}{2}$	$\frac{3}{2}$	$\sqrt{3}$			
		b	$-(\text{conj } V_B)I_c$	$\frac{3}{4}$	$\frac{3\sqrt{3}}{4}$	$-\frac{3}{2}$	$\frac{\sqrt{3}}{4}$	$\frac{3}{4}$	$-\frac{\sqrt{3}}{2}$			
		c	$-(\text{conj } V_C)I_a$	$\frac{3}{2}$	0	$\frac{3}{2}$	$-\frac{\sqrt{3}}{2}$	0	$-\frac{\sqrt{3}}{2}$			
		Polyphase		$\frac{9}{4}$	$\frac{3\sqrt{3}}{4}$	0	$-\frac{3\sqrt{3}}{4}$	$\frac{9}{4}$	0			
Wye-Voltage Delta-Current Connections	90-degree	a	$(\text{conj } V_a)I_A$	0	$-\sqrt{3}$	0	0	0	0	0	$-\sqrt{3}$	
		b	$(\text{conj } V_b)I_B$	$-\frac{3}{4}$	$-\frac{\sqrt{3}}{4}$	0	$\frac{3\sqrt{3}}{4}$	$\frac{3}{4}$	0	$\frac{3}{2}$	$\frac{\sqrt{3}}{2}$	0
		c	$(\text{conj } V_c)I_C$	$\frac{3}{4}$	$-\frac{\sqrt{3}}{4}$	0	$\frac{3\sqrt{3}}{4}$	$-\frac{3}{4}$	0	$-\frac{3}{2}$	$\frac{\sqrt{3}}{2}$	0
		Polyphase		0	$-\frac{3\sqrt{3}}{2}$	0	$\frac{3\sqrt{3}}{2}$	0	0	0	0	0
Delta-Voltage Delta-Current Connections	60-degree	a	$-(\text{conj } V_A)I_B$	0	0	0	$\frac{3\sqrt{3}}{2}$	$\frac{3}{2}$				
		b	$-(\text{conj } V_B)I_C$	$\frac{9}{4}$	$-\frac{3\sqrt{3}}{4}$	0	$\frac{3\sqrt{3}}{4}$	$-\frac{3}{4}$				
		c	$-(\text{conj } V_C)I_A$	0	$-\frac{3\sqrt{3}}{2}$	0	0	$\frac{3}{2}$				
		Polyphase		$\frac{9}{4}$	$-\frac{9\sqrt{3}}{4}$	0	$\frac{9\sqrt{3}}{4}$	$\frac{9}{4}$				
	60-degree alternate	a	$-(\text{conj } V_A)I_C$	0	0	0	$-\frac{3\sqrt{3}}{2}$	$\frac{3}{2}$				
		b	$-(\text{conj } V_B)I_A$	0	$\frac{3\sqrt{3}}{2}$	0	0	$\frac{3}{2}$				
		c	$-(\text{conj } V_C)I_B$	$\frac{9}{4}$	$\frac{3\sqrt{3}}{4}$	0	$-\frac{3\sqrt{3}}{4}$	$-\frac{3}{4}$				
		Polyphase		$\frac{9}{4}$	$\frac{9\sqrt{3}}{4}$	0	$-\frac{9\sqrt{3}}{4}$	$\frac{9}{4}$				

* To find the modified symmetrical-component expression for the total vector volt-ampere input to any relay, take the sum of all the column headings, each multiplied by the corresponding coefficient taken from the row opposite the relay considered. The voltage and current components in the network at the point where the relay is located must be used in these expressions.

ferently is indicated by the expressions of Table I. The cross-product terms (such as $(\text{conj } V_1)I_2$, $(\text{conj } V_1)I_0$, and so forth) are identical in all three expressions for a particular connection except that they are rotated through different angles before being combined with the other terms. Thus, negative torque may be produced in one relay even though the other two operate correctly. Since the polyphase relay consists of three single-phase elements mounted on a common shaft, the cross-product terms in the volt-ampere expressions cancel. The relay is governed completely by the three terms $(\text{conj } V_1)I_1$, $(\text{conj } V_2)I_2$, and $(\text{conj } V_0)I_0$. The latter term is zero in all cases except for ground faults, and then appears only in the expressions for the wye-voltage wye-current connections.

The wye-voltage delta-current connections give exactly the same relay operation as the corresponding delta-voltage wye-current connections. Therefore, neither set has any advantage over the other. Likewise, for faults not involving ground, the wye-voltage wye-current connections have no advantage over the delta-voltage delta-current connections. On ground faults the wye-voltage wye-current connections would give somewhat better performance. The angle of the $(\text{conj } V_0)I_0$ term usually is such that it represents positive torque, and the relay is somewhat more free from the disturbing influence of normal load current.

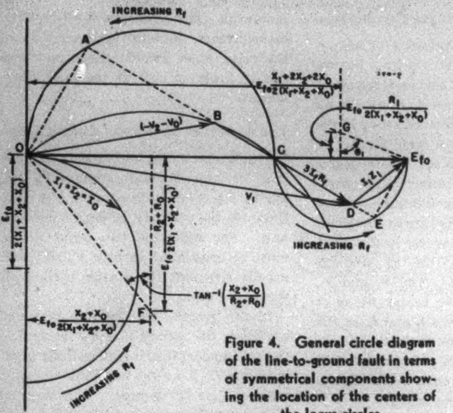

INCREASING R_f
A
$E_{f0}\frac{X_1+2X_2+2X_0}{2(X_1+X_2+X_0)}$
$E_{f0}\frac{R_f}{2(X_1+X_2+X_0)}$
B
$(-V_2-V_0)$
G
O
G
E_{f0}
$I_1=I_2=I_0$
$3I_1R_f$
I_1Z_1
V_1
D
E
INCREASING R_f
$\frac{E_{f0}}{2(X_1+X_2+X_0)}$
$E_{f0}\frac{R_2+R_0}{2(X_1+X_2+X_0)}$
$\tan^{-1}\left(\frac{X_2+X_0}{R_2+R_0}\right)$
F
$E_{f0}\frac{X_2+X_0}{2(X_1+X_2+X_0)}$
θ_1
INCREASING R_f

Figure 4. General circle diagram of the line-to-ground fault in terms of symmetrical components showing the location of the centers of the locus circles

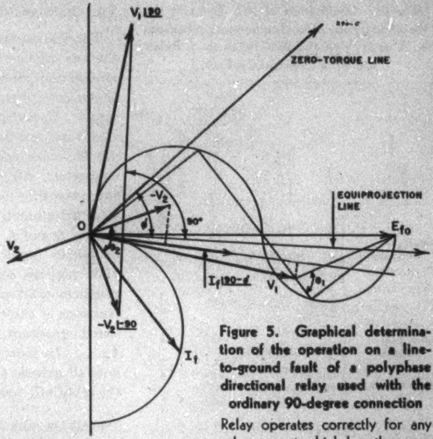


Figure 5. Graphical determination of the operation on a line-to-ground fault of a polyphase directional relay used with the ordinary 90-degree connection

Relay operates correctly for any relay current which lags the zero-torque line by less than 180 degrees

The relative merits of the 30-degree, 60-degree, and 90-degree connections may be studied by application of the circle diagram. The conditions existing during a line-to-ground fault are illustrated by the diagram of Figure 3. For the line-to-line fault, the diagram is similar, the essential difference being that point C is situated at or near the center of the line OE_{f0}, and V_0 does not appear at all.

Since the positive-sequence voltage can never be less in magnitude than the negative-sequence voltage, the zero-sequence voltage, or the vector sum, the term $(\text{conj } V_1)I_1$ usually will govern the relay behavior. A possible exception exists with the wye-wye connections. For a ground fault on a circuit which is grounded at one end only, the zero-sequence current at the relay might be considerably greater than either of the other two components, in which case the $(\text{conj } V_0)I_0$ term would take precedence. For this special condition the relay would be less subject to incorrect operation than in more general networks.

For the 90-degree connections, the volt-ampere input to the relay is $k[(\text{conj } V_1)I_1\underline{|90}+(\text{conj } V_2)I_2\underline{|-90}]$. Neglecting load current, I_1 is very nearly equal to I_2 in any actual power network. The expression therefore may be written $kI_1(\text{conj } V_1-\text{conj } V_2)\underline{|90}$. When only two terms are present in the volt-ampere expression, the relay torque may be obtained from the projections of the two voltages on the current vector rotated counterclockwise through the angle ϕ. For almost all conditions, V_1 will lag E_{f0} by an angle of from 0 to 45 degrees, and $-V_2$ will lie somewhere in the first quadrant as shown in Figure 3. Thus, the 90-degree connections will nearly always give correct operation if the relay angle ϕ is properly chosen.

The ordinary 30- and 60-degree connections give performance comparable with that of the 90-degree connections. The voltage V_1 will, in general, lag the voltage $-V_2$, so that rotation of V_1 counterclockwise and $-V_2$ clockwise tends to interchange their relative positions without appreciably affecting the torque produced. The relay angle ϕ should be zero or slightly positive for the 30-degree connection and positive for the 60- and 90-degree connections, perhaps 45 to 50 degrees for the latter, to insure correct relay operation for the greatest number of practical situations.

The 30- and 60-degree alternate connections are definitely inferior to the ordinary connections. To obtain the relay torque by means of the circle diagram, the two voltage vectors must be rotated in directions opposite to those for the ordinary connections. Thus, the voltages become widely separated in phase. One of the torque terms will usually be negative and the relay more subject to incorrect operation.

Modified Symmetrical Components and Relay Operation

In order that the relative merits of the several relay connections may be ascertained and compared when single-phase relay elements are considered, the system network impedances must be assigned different values, and the direction of the relay torque ascertained for each condition. The criterion as to the best connection is the range or allowable variation in both the magnitude and angle of the impedances that will not result in incorrect relay operation.

The direction of the relay torque is governed by the angle of the relay vector volt-ampere input for the particular relay and connection considered. The resultant torque is the algebraic sum of the separate components represented by the terms in the relay volt-ampere expression.

Values of network impedances which represent limiting cases from the standpoint of relay behavior are the only ones that need be considered critically when the effects of different forms of network dissymmetry are evident from the circle diagram. This is the case only when the relay volt-ampere expression contains very few terms. When symmetrical components are used for the determination of relay behavior in a generalized system network, the effects of dissymmetries are very obscure since many of the symmetrical component volt-ampere expressions contain as many as nine terms. Modified symmetrical components[2] not only provide a simpler circle diagram but also reduce considerably the number of terms in the relay volt-ampere expressions. For instance, the symmetrical-component expression for relay a of the ordinary 90-degree connection contains six terms as compared with two terms in the modified symmetrical-component expression. Thus, the use of modified symmetrical components makes possible a much more accurate evaluation of the relative merits of the several relay connections.

The circle diagram of the line-to-ground fault based upon the circuit of Figure 2b in terms of modified symmetrical components is illustrated in Figure 6. The

Table III. Coefficients of All Terms in the Modified Symmetrical-Component Expressions for Vector Volt-Ampere Input to a Relay During a Line-to-Line Fault

Relay Connection	Relay	Current-Voltage Product	Volt-Ampere* Terms		Common Factor
			$(\text{conj } V_\alpha)I_\beta$	$(\text{conj } V_\beta)I_\beta$	
60-degree ordinary Wye-voltage Wye-current	a	$-(\text{conj } V_a)I_b$	$-\frac{1}{\sqrt{3}}$	0	$\frac{3}{2}$
	b	$-(\text{conj } V_b)I_c$	$-\frac{1}{\sqrt{3}}$	1	$\frac{3}{4}$
	c	$-(\text{conj } V_c)I_a$	0	0	
	Polyphase		$-\sqrt{3}$	1	$\frac{3}{4}$
60-degree alternate Wye-voltage Wye-current	a	$-(\text{conj } V_a)I_c$	$\frac{1}{\sqrt{3}}$	0	$\frac{3}{2}$
	b	$-(\text{conj } V_b)I_a$	0	0	
	c	$-(\text{conj } V_c)I_b$	$\frac{1}{\sqrt{3}}$	1	$\frac{3}{4}$
	Polyphase		$\sqrt{3}$	1	$\frac{3}{4}$
90-degree ordinary Delta-voltage Wye-current	a	$-(\text{conj } V_A)I_a$	0	0	
	b	$-(\text{conj } V_B)I_b$	$-\sqrt{3}$	-1	$\frac{3}{4}$
	c	$-(\text{conj } V_C)I_c$	$-\sqrt{3}$	1	$\frac{3}{4}$
	Polyphase		$-\sqrt{3}$	0	$\frac{3}{2}$
30-degree ordinary Delta-voltage Wye-current	a	$(\text{conj } V_A(I_c$	0	1	$\frac{3}{2}$
	b	$(\text{conj } V_B)I_a$	0	0	
	c	$(\text{conj } V_C)I_b$	$-\sqrt{3}$	1	$\frac{3}{4}$
	Polyphase		$-\frac{1}{\sqrt{3}}$	1	$\frac{9}{4}$
30-degree alternate Delta-voltage Wye-current	a	$-(\text{conj } V_A)I_b$	0	1	$\frac{3}{2}$
	b	$-(\text{conj } V_B)I_c$	$\sqrt{3}$	1	$\frac{3}{4}$
	c	$-(\text{conj } V_C)I_a$	0	0	
	Polyphase		$\frac{1}{\sqrt{3}}$	1	$\frac{9}{4}$
90-degree Wye-voltage Delta-current	a	$(\text{conj } V_a)I_A$	$-\sqrt{3}$	0	1
	b	$(\text{conj } V_b)I_B$	$-\frac{1}{\sqrt{3}}$	1	$\frac{3}{4}$
	c	$(\text{conj } V_c)I_C$	$-\frac{1}{\sqrt{3}}$	-1	$\frac{3}{4}$
	Polyphase		$-\sqrt{3}$	0	$\frac{3}{2}$
60-degree Delta-voltage Delta-current	a	$-(\text{conj } V_A)I_B$	0	1	$\frac{3}{2}$
	b	$-(\text{conj } V_B)I_C$	$-\sqrt{3}$	-1	$\frac{3}{4}$
	c	$-(\text{conj } V_C)I_A$	$-\sqrt{3}$	1	$\frac{3}{2}$
	Polyphase		$-\sqrt{3}$	1	$\frac{9}{4}$
60-degree alternate Delta-voltage Delta-current	a	$-(\text{conj } V_A)I_C$	0	1	$\frac{3}{2}$
	b	$-(\text{conj } V_B)I_A$	$\sqrt{3}$	1	$\frac{3}{2}$
	c	$-(\text{conj } V_C)I_B$	$\sqrt{3}$	-1	$\frac{3}{4}$
	Polyphase		$\sqrt{3}$	1	$\frac{9}{4}$

* To find the modified symmetrical-component expression for the total vector volt-ampere input to any relay, take the sum of the two column headings, each multiplied by the common factor and the corresponding coefficient taken from the row opposite the relay considered. The voltage and current components in the network at the point where the relay is located must be used in these expressions.

point C is determined by the ratio $OC/CE_{\alpha-f_0}=(X_0/2)/X_1$. Since the β-network is open, the voltage V_β is constant and equal to $-j(V_1-V_2)=-jE_{f_0}$ for any value of fault resistance. For completeness, this vector is superposed on the diagram.

The Ordinary 60-Degree Connection

The volt-ampere expression for relay c, the one principally affected by a line-to-ground fault, does not lend itself readily to general treatment, since it consists of six terms. However, a detailed study of the conditions existing in a large number of specific cases indicates that the 60-degree connection will give correct relay operation except for extreme conditions of network dissymmetry. As long as the vector sum of I_0 and I_α is a vector in the third or fourth quadrants, the V_β products yield positive torque. The V_0 and V_α products yield positive torque for most positions of the currents I_0 and I_α in the fourth quadrant, particularly those near $E_{\alpha-f_0}$. In most cases the currents will not fall outside the fourth quadrant and the relay will operate correctly.

The 60-Degree Alternate Connection

This connection is inferior to the ordinary 60-degree connection. Relay b, the one subject to operation on a line-to-ground fault, has exactly the same volt-ampere expression as relay c for the ordinary 60-degree connection, except for the sign of the two terms involving V_β. Thus, regardless of the value chosen for the relay angle, some of the torque terms will be negative, and the relay subject to incorrect operation.

The Ordinary 90-Degree Connection

On a line-to-ground fault, relay a is the one principally affected. The volt-ampere expression consists of two terms which involve V_β, I_α, and I_0. Since V_β is constant, the variations of I_α and I_0 are all that need be considered. If the power network is symmetrical so that the relay components of current are inphase with the fault components, incorrect relay operation can never occur, if the relay angle ϕ is between zero and 90 degrees. It can be shown that the current I_M must always lie inside a semicardioid along the vertical axis (Figure 7), and inside a semicircle in the third quadrant. In most instances the relay current will lie in the fourth quadrant, but, because of network dissymmetries, may fall outside. If the α network is mostly reactive, and the zero network resistive as viewed one way from the point of fault and reactive as viewed the other way, the zero-sequence current on the reactive side will lie in the fourth quadrant

For the 90-degree connection the impedance between the relay and point of fault has no effect on the relay performance except as a secondary effect arising from modifications in V_β at the relay due to the flow of normal load current in the β network. Since the relay is affected by load currents, the total component I_α at the relay must include the component due to loads as well as the fault component.

The Ordinary 30-Degree Connection

The terms of the volt-ampere expression for relay b which contain V_β are identical (except for the factor $^1/_2$) with those for the ordinary 90-degree connection. The additional two terms which must be considered involve V_α and will usually represent the greater portion of the relay torque.

In the general case for any network condition, the voltage V_α must lie within the two semicardioidal regions illustrated in Figure 7. Thus the maximum angular range of V_α is 270 degrees. This range is shifted by 90 degrees from the 270-degree current range and would seem to indicate that the relay angle should be approximately equal to +90 degrees. For the usual practical network conditions however, the voltage V_α will lag slightly the voltage $E_{\alpha-f_0}$, and the current will lie in the fourth quadrant at an angle of perhaps 60 to 80 degrees lagging $E_{\alpha-f_0}$. Therefore, the relay angle should be only slightly positive or zero for most cases. The torque represented by the last two terms will, in general, be positive and add to that of the other two.

The Alternate 30-Degree Connection

The volt-ampere expression for the alternate 30-degree connection—relay c—is identical to that of the ordinary 30-degree connection—relay b—except for the sign of the two terms containing V_β. Thus, the torque represented by these terms will in most cases be of opposite sign from that represented by the other two. This connection is more subject to incorrect operation than the ordinary 30-degree connection.

The 90-Degree Wye-Voltage Delta-Current Connection

For this connection, relays b and c both operate on a line-to-ground fault, but in different ways as shown by the expressions of Table I. Except for the second term, one expression is the negative of the other. In many instances the second term would represent only a minor part of the total relay torque, and the two relays would function oppositely. This effect might occur even in a symmetrical network with the relay located near the fault point. This connection is definitely unreliable on ground faults. On other types of fault where the current I_β is present, the torque represented by the I_β

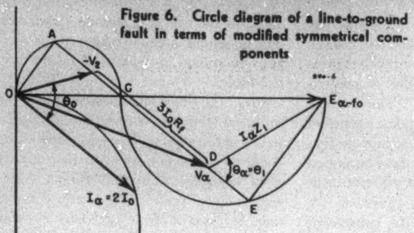

A
$-V_2$
O
C
θ_0
$E_{\alpha-fo}$
$3I_0R_1$
$I_\alpha Z_1$
D
V_α
$\theta_\alpha - \theta_1$
$I_\alpha = 2I_0$
E

Figure 6. Circle diagram of a line-to-ground fault in terms of modified symmetrical components

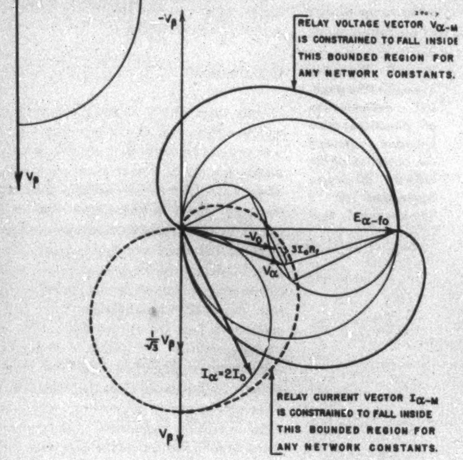

$-V_\beta$
RELAY VOLTAGE VECTOR $V_{\alpha-M}$ IS CONSTRAINED TO FALL INSIDE THIS BOUNDED REGION FOR ANY NETWORK CONSTANTS.
V_β
$E_{\alpha-fo}$
$-V_2$
$3I_0R_1$
V_α
$\frac{1}{\sqrt{3}}V_\beta$
$I_\alpha = 2I_0$
RELAY CURRENT VECTOR $I_{\alpha-M}$ IS CONSTRAINED TO FALL INSIDE THIS BOUNDED REGION FOR ANY NETWORK CONSTANTS.
V_β

Figure 7 (left). Circle diagram showing the limits of variation of the relay current and voltage for either a line-to-ground or line-to-line fault expressed in modified symmetrical components

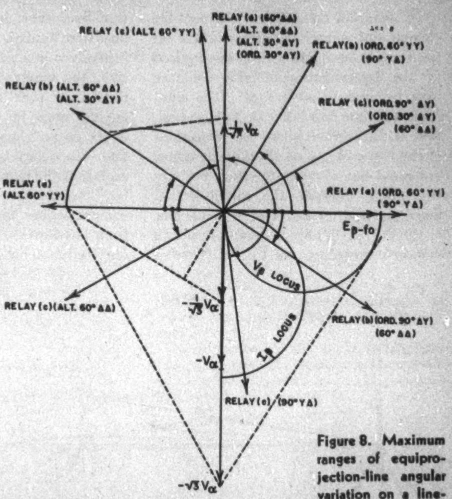

RELAY (c) (ALT. 60° YY)
RELAY (a) (60° ΔΔ) (ALT. 60° ΔΔ) (ALT. 30° ΔY) (ORD. 30° ΔY)
RELAY (b) (ORD. 60° YY) (90° YΔ)
RELAY (b) (ALT. 60° ΔΔ) (ALT. 30° ΔY)
RELAY (c) (ORD. 90° ΔY) (ORD. 30° ΔY) (60° ΔΔ)
$\frac{1}{\sqrt{3}}V_\alpha$
RELAY (a) (ALT. 60° YY)
RELAY (a) (ORD. 60° YY) (90° YΔ)
$E_{\beta-fo}$
V_β LOCUS
RELAY (c) (ALT. 60° ΔΔ)
$-\frac{1}{\sqrt{3}}V_\alpha$
RELAY (b) (ORD. 90° ΔY) (60° ΔΔ)
I_β LOCUS
$-V_\alpha$
RELAY (c) (90° YΔ)
$-\sqrt{3}V_\alpha$

Figure 8. Maximum ranges of equiprojection-line angular variation on a line-to-line fault as influenced by fault or network resistances

The arcs with arrowheads indicate the maximum angular range of the equiprojection lines of each relay of the connections listed in Table III. Limitations as to the allowable variation of the network constants are $Z_z = 0$ and angle of Z_{Rf} = angle of Z_1

terms in the general expressions would not be zero and, since these components of torque are of the same sign for both relays, better behavior of the two relays would be obtained.

THE 60-DEGREE DELTA-VOLTAGE DELTA-CURRENT CONNECTION

Relay *a* for the delta-voltage delta-current connection behaves in much the same way as relay *a* for the ordinary 90-degree connection, the chief difference being that for the former there is no zero-sequence torque term. In some ways this is an advantage, because the operation of the relay is independent of the zero-sequence network, except as a secondary effect arising because of modifications in the current I_α. On the other hand, this relay connection would give operation more subject to the influence of normal load current, because it operates on V_β and I_α only. Normal load current reduces to equal components of I_α and I_β in time quadrature with each other.

Relay *b* behaves in much the same way as relay *a*. In general I_α and V_α will both lie in the fourth quadrant, and the torque represented by the first term will be positive and add to that of the second. For both relays the relay angle ϕ should be positive, perhaps about 45 degrees for usual network conditions.

THE 60-DEGREE ALTERNATE DELTA-VOLTAGE DELTA-CURRENT CONNECTION

Comparing the volt-ampere expressions for this connection with those for the 60-degree delta-voltage delta-current connection, the signs are opposite for those terms involving the voltage V_β. Thus, the two relays tend to function oppositely on line-to-ground faults.

Relay *c* is actuated by two opposing torques, since the two terms will, in general, represent torque of opposite sign. The absolute values of the two vector volt-ampere components are of comparable magnitude, and incorrect operation of this relay is quite likely to occur. This connection is definitely inferior to the 60-degree delta-voltage delta-current connection.

Relay Operation on a Line-to-Line Fault

Circle diagrams for the line-to-line type of fault are quite similar to those for the line-to-ground fault, although somewhat simpler, because they do not involve the zero-sequence network. Since, in the equivalent representation of Figure 2*d*, the α network is open, the voltage V_α is constant. The voltage V_β follows the locus circle which has $E_{\beta-fo}$ as the diameter.

When the conditions for a line-to-line fault are substituted into the general volt-ampere expressions of Table II, each reduces to two terms. By factoring out the proper numerical factor, the coefficients of the terms involving V_β are reduced to unity, and the coefficients of the terms involving V_α to either $\sqrt{3}$ or $1/\sqrt{3}$. Since V_α is constant and equal to $E_{\alpha-fo}$ (except as influenced by normal load current), this procedure aids in the comparison of the various terms. The simplified expressions are given in Table III.

For the relay to operate correctly, the sum of the two torque components must be positive. Since the same current is involved in both components, it is necessary only that the algebraic sum of the projections of the two voltages on the current vector be positive to meet this condition.

To ascertain the effect of the possible current and voltage variations on the relay torque, the equiprojection-line method for the determination of relay behavior may be used to advantage. The equiprojection line represents that phase position of the current where the projections of the two voltages on the current vector are equal but of opposite sign. For any two given voltages, the equiprojection line is drawn from the origin perpendicular to the line which joins the ends of the two voltage vectors after one has been rotated through 180 degrees. Since the effect of the relay angle ϕ may be included by rotation of the equiprojection line through the angle ϕ, the maximum angular variations of the equiprojection lines may be studied independent of the relay angle. The proper value of the relay angle for any one of the different connections is such that the relay will yield positive torque for the range of currents most frequently encountered in actual power systems.

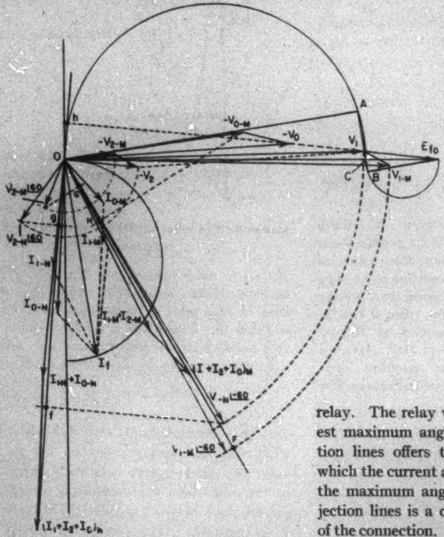


Figure 9. Illustrative example of the graphical determination of directional-relay behavior showing the operation of the ordinary 60-degree connection on a line-to-ground fault

For the case where the impedance $\boldsymbol{Z}_x$ is zero and the impedances $\boldsymbol{Z}_{Rf}$ and $\boldsymbol{Z}_1$ have the same angle, the maximum angular ranges of the equiprojection lines may be obtained from Figure 8. For this case, the current vector must lie either in the first or fourth quadrant, but usually in the fourth. The positions of the equiprojection lines indicated are the limiting positions, the angular ranges being indicated by arrows. The dotted construction lines show how the equiprojection lines were located. When just one voltage is involved in a relay volt-ampere expression, the equiprojection line is perpendicular to that voltage. The current angular range for correct relay operation is 180 degrees lagging the equiprojection line. Since the relay angle must be chosen such that the relay current will give positive torque, the angular range of the equiprojection line must be subtracted from 180 degrees to give the range of current variation for correct operation of the relay. The relay which yields the smallest maximum angle between equiprojection lines offers the largest range over which the current angle may vary. Thus, the maximum angular range of equiprojection lines is a criterion of the quality of the connection. The maximum angular ranges as determined from Figure 8 for the above-mentioned network conditions are given in the first column of Table A.

The figures of this table indicate that the three ordinary connections, the alternate 60-degree connection, and the 60-degree delta-voltage delta-current connection are the best. The latter connection gives very satisfactory operation on line-to-ground faults but is somewhat inferior on line-to-line faults. The converse is true for the alternate 60-degree connection.

When the restriction is removed that the impedances $\boldsymbol{Z}_{Rf}$ and $\boldsymbol{Z}_1$ have the same angle, the voltage $\boldsymbol{V}_B$ may lie anywhere within a semicircle above $\boldsymbol{E}_{B-f_0}$ and a semicardioid below $\boldsymbol{E}_{B-f_0}$. When the other restriction, that the impedance $\boldsymbol{Z}_x$ is zero, is removed, the maximum angular ranges are still further increased. The voltage $\boldsymbol{V}_B$ may then lie anywhere within a semicardioid below $\boldsymbol{E}_{B-f_0}$ in the fourth quadrant and a semicardioid above $\boldsymbol{E}_{B-f_0}$ in the first and second quadrants. The maximum angular ranges of the equiprojection lines are increased as shown in the table.

From the preceding considerations, the ordinary 90-degree connection is superior to any of the others. However, most of the connections are not so bad as they appear from the equiprojection-line angular variations, since these ranges are possible only for conditions of extreme dissymmetry which do not frequently occur in actual power networks.

Conclusions

Only three of the 12 relay connections which utilize wye or delta voltages and wye or delta currents seem to have definite merits for directional-relay applications. These three connections are the ordinary 60-degree (wye voltages and wye currents), the ordinary 90-degree (delta voltages and wye currents), and the ordinary 30-degree (delta voltages and wye currents). While it is not possible to state definitely which of these three connections is best for all possible faults, it would seem that the ordinary 90-degree connection is somewhat more free from disturbing influences than the other two. None of the twelve connections will give relay operation essentially independent of the type of fault as is the case with the 90-degree delta-voltage delta-current connection when applied to distance relaying.

For all the various connections, normal load current is the most disturbing influence with regard to correct relay operation. In many instances it may be impossible to obtain satisfactory results on ground faults without the use of "ground relays" which operate on negative- or zero-sequence quantities only. Some of the other factors besides the type of relay connection and normal load current in the system which influence directional relay operation are:

1. Type of fault.
2. Different angles in the positive-, negative-, and zero-sequence impedances as viewed from the fault point in the same direction from the fault.
3. Different angles in the impedances as viewed from the fault point in opposite directions in either sequence network.
4. Impedance between the relay and point of fault, particularly if the angle of this impedance is different from that of the total impedance as viewed from the point of fault.
5. Fault resistance.
6. Mutual impedance between the two parts of the system on opposite sides of the fault in networks of like sequence. Coupling of this nature is present in all except pure radial power networks.

Table A

	Maximum Angular Range of Equiprojection Lines (Degrees)		
	Network Conditions and Restrictions		
Relay Connections	$Z_s=0$ Angle of Z_{Rf} = Angle of Z_1	$Z_s=0$	None
Ordinary 60-degree	60	82	127
Alternate 60-degree	80	108	116
Ordinary 90-degree	60	66	74
Ordinary 30-degree	90	180	270
Alternate 30-degree	180	180	270
90-degree wye-voltage delta-current	120	130	130
60-degree delta-voltage delta-current	115	180	270
Alternate 60-degree delta-voltage delta-current	180	180	270

While most of the factors are not of sufficient importance to cause incorrect relay operation if only one influence were present, they are cumulative in the sense that several of these factors present at the same time may very easily result in incorrect directional relay operation.

Appendix A. Derivation of the Circle Diagram for the Line-to-Ground Fault in Terms of Symmetrical Components

From the conditions existing at the point of fault:

$$\mathbf{I}_f=\frac{\mathbf{E}_{fo}}{\mathbf{Z}_1+\mathbf{Z}_2+\mathbf{Z}_0+3R_f}$$
$$=\frac{E_{fo}}{R_1+R_2+R_0+3R_f+j(X_1+X_2+X_0)} \quad (1)$$

Letting the subscripts x and y indicate components of $\mathbf{I}_f$ in the X and Y directions respectively, one obtains by cross-multiplying and equating the reals and imaginaries

$$I_x(R_1+R_2+R_0+3R_f)-I_y(X_1+X_2+X_0)=E_{fo} \quad (2)$$

$$I_x(X_1+X_2+X_0)+I_y(R_1+R_2+R_0+3R_f)=0 \quad (3)$$

Elimination of R_f from these two equations gives the current-locus equation:

$$I_x^2+\left[I_y+\frac{E_{fo}}{2(X_1+X_2+X_0)}\right]^2=\left[\frac{E_{fo}}{2(X_1+X_2+X_0)}\right]^2 \quad (4)$$

This equation defines a circle which passes through the origin. The center is on the Y axis at the point

$$\left[0,\frac{-E_{fo}}{2(X_1+X_2+X_0)}\right]$$

The positive-sequence voltage $\mathbf{V}_1$ is defined by the equation

$$\mathbf{V}_1=\mathbf{E}_{fo}-\mathbf{I}_1\mathbf{Z}_1 \quad (5)$$

or

$$(V_{1-x}+jV_{1-y})=E_{fo}-\frac{E_{fo}(R_1+jX_1)}{(R_1+R_2+R_0+3R_f)+j(X_1+X_2+X_0)} \quad (6)$$

Cross-multiplying, equating the reals and imaginaries, and eliminating R_f from the resultant two equations gives the locus equation of the positive-sequence voltage.

$$\left[V_{1-x}-E_{fo}\frac{X_1+2X_2+2X_0}{2(X_1+X_2+X_0)}\right]^2+\left[V_{1-y}-E_{fo}\frac{R_1}{2(X_1+X_2+X_0)}\right]^2$$
$$=E_{fo}^2\left[\frac{R_1^2+X_1^2}{4(X_1+X_2+X_0)^2}\right]=\left[E_{fo}\frac{Z_1}{2(X_1+X_2+X_0)}\right]^2 \quad (7)$$

This equation defines a circle with center at the point

$$\left[\frac{E_{fo}(X_1+2X_2+2X_0)}{2(X_1+X_2+X_0)},\frac{E_{fo}R_1}{2(X_1+X_2+X_0)}\right]$$

and of radius

$$\frac{E_{fo}Z_1}{2(X_1+X_2+X_0)}$$

It intersects the X axis at the two points

$[E_{fo}, 0]$

and

$$\left[\frac{E_{fo}(X_2+X_0)}{X_1+X_2+X_0},0\right]$$

These two intercepts, being independent of R_1, R_2, and R_0, depend only upon the *reactances* of the network.

It is more convenient to treat the vector sum of the negative- and zero-sequence voltages as a single voltage. The separate components may then be obtained easily after the diagram is thus constructed. For convenience, let

$$(-\mathbf{V}_2-\mathbf{V}_0)=\mathbf{V}=\mathbf{I}_f(\mathbf{Z}_2+\mathbf{Z}_0) \quad (8)$$

or

$$(V_x+jV_y)=\frac{E_{fo}[R_2+R_0+j(X_2+X_0)]}{R_1+R_2+R_0+3R_f+j(X_1+X_2+X_0)} \quad (9)$$

Cross-multiplying, equating the reals and imaginaries, and eliminating R_f from the resultant two equations gives the locus equation of the negative of the vector sum of the negative- and zero-sequence voltages.

$$\left[V_x-E_{fo}\frac{X_2+X_0}{2(X_1+X_2+X_0)}\right]^2+\left[V_y+E_{fo}\frac{R_2+R_0}{2(X_1+X_2+X_0)}\right]^2$$
$$=E_{fo}^2\left[\frac{(R_2+R_0)^2+(X_2+X_0)^2}{4(X_1+X_2+X_0)^2}\right]=\left[E_{fo}\frac{|\mathbf{Z}_2+\mathbf{Z}_0|}{2(X_1+X_2+X_0)}\right]^2 \quad (10)$$

This equation defines a circle with center at the point

$$\left[\frac{E_{fo}(X_2+X_0)}{2(X_1+X_2+X_0)},\frac{-E_{fo}(R_2+R_0)}{2(X_1+X_2+X_0)}\right]$$

and of radius $\dfrac{E_{fo}|\mathbf{Z}_2+\mathbf{Z}_0|}{2(X_1+X_2+X_0)}$

It intersects the X axis at the two points

[0, 0]

and

$$\left[\frac{E_{fo}(X_2+X_0)}{2(X_1+X_2+X_0)},0\right]$$

which, being independent of R_1, R_2, and R_0, depend only upon the *reactances* of the network.

Simultaneous solution of the two voltage locus equations—the equation of $\mathbf{V}_1$ and the equation of $(-\mathbf{V}_2-\mathbf{V}_0)$—gives the points of intersection of the two circles

$$\left[\frac{E_{fo}[(X_1+X_2+X_0)(X_2+X_0)+(R_1+R_2+R_0)(R_2+R_0)]}{(R_1+R_2+R_0)^2+(X_1+X_2+X_0)^2},\right.$$
$$\left.\frac{E_{fo}[(R_1+R_2+R_0)(X_2+X_0)-(X_1+X_2+X_0)(R_2+R_0)]}{(R_1+R_2+R_0)^2+(X_1+X_2+X_0)^2}\right]$$

and

$$\left[\frac{E_{fo}(X_2+X_0)}{X_1+X_2+X_0},0\right]$$

The second point is the point of intersection of the two voltage locus equations with the X axis. Since this point is determined only by the reactances of the three sequence networks, it is independent of the resistances of the networks, as well as the fault resistance. As the resistances of the sequence networks approach zero, the two points of intersection approach each other, and the two locus circles approach tangency. The second point is always on the X axis, while the first may be either above or below the X axis depending upon the relative values of the sequence-network resistances and reactances. The first point represents conditions on the loci when the fault resistance is zero.

The derivation of the circle diagram for the line-to-line fault in terms of symmetrical components, and the derivation of circle diagrams for both types of faults in terms of modified symmetrical components follow closely the method used for the derivation of the circle diagram of the line-to-ground fault. All are based upon the circuit diagrams illustrated in Figure 2.

Appendix B. Illustrative Example of the Graphical Determination of Directional-Relay Behavior

To illustrate the use of circle diagrams and symmetrical components for the determination of directional-relay behavior, the following example is given in which the impedances of the equivalent-network representation of Figure 1 are per-unit values taken from an actual power network.

$\mathbf{Z}_{1-Z} = \mathbf{Z}_{2-Z} = 0.020|\underline{90.0}$ $\quad \mathbf{Z}_{0-Z} = 0.240|\underline{90.0}$

$\mathbf{Z}_{1-X} = \mathbf{Z}_{2-X} = 0.128|\underline{70.5}$ $\quad \mathbf{Z}_{0-X} = 0.214|\underline{52.6}$

$\mathbf{Z}_{1-Rf} = \mathbf{Z}_{2-Rf} = 0.097|\underline{34.0}$ $\quad \mathbf{Z}_{0-Rf} = 0.238|\underline{32.0}$

$\mathbf{Z}_{1-M} = \mathbf{Z}_{2-M} = 0.214|\underline{54.9}$ $\quad \mathbf{Z}_{0-M} = 0.489|\underline{40.8}$

$\mathbf{Z}_{1-N} = \mathbf{Z}_{2-N} = 0.188|\underline{88.5}$ $\quad \mathbf{Z}_{0-N} = 0.188|\underline{88.5}$

$\mathbf{Z}_{1-Y} = \mathbf{Z}_{2-Y} = 0.188|\underline{88.5}$ $\quad \mathbf{Z}_{0-Y} = 0.188|\underline{88.5}$

$\mathbf{Z}_1 = \mathbf{Z}_2 = 0.124|\underline{75.5}$ $\quad \mathbf{Z}_0 = 0.383|\underline{84.6}$

$\mathbf{Z}_1+\mathbf{Z}_2+\mathbf{Z}_0 = 0.629|\underline{81.0} = 0.098+j0.621$

$\mathbf{Z}_2+\mathbf{Z}_0 = 0.505|\underline{82.4} = 0.067+j0.501$

Type of fault: Line-to-ground
Relay connection: Ordinary 60-degree
Relay angle ϕ: 0 degrees

Construction of the Circle Diagram

The purpose of the circle diagram is to obtain the relative magnitude and phase of each of the sequence voltages and currents at the relay in the form of a vector diagram. The direction of the relay torque then is found by graphical evaluation of the torque components represented by the terms of the volt-ampere expressions of Table I.

In Figure 9, the vector $\mathbf{E}_{f_0}$ is laid off horizontally to scale, and the point C located from the ratio $OC/OE_{f_0} = (X_2+X_0)/(X_1+X_2+X_0) = 0.501/0.621 = 0.806$. The center of the fault current-locus circle is on the vertical axis below O by the amount $E_{f_0}/[2\times(X_1+X_2+X_0)] = 0.805$. This point may be taken at any convenient distance below O, and the actual current scale left undetermined unless the current magnitude is required for other purposes.

The fault current vector $\mathbf{I}_f$ is drawn lagging $\mathbf{E}_{f_0}$ by 81.0 degrees, the angle of the impedance of the three sequence networks in series. The lines OA and BE_{f_0} are perpendicular to the fault current, and the line ACB is parallel to the fault current. The line $E_{f_0}V_1$ is drawn at 75.5 degrees with AB to locate the end of the positive-sequence voltage vector $\mathbf{V}_1$. Since the fault resistance is zero, this point also represents the terminus of the vector sum of the negative- and zero-sequence voltages $-\mathbf{V}_2$ and $-\mathbf{V}_0$.

The voltages $-\mathbf{V}_2$ and $-\mathbf{V}_0$ are drawn leading the fault current by 75.5, and 84.6 degrees respectively. The length of these vectors are such that they add vectorially to give the vector $\mathbf{V}_1$.

The current components $\mathbf{I}_{2-M}$, and $\mathbf{I}_{2-N}$ (equal to $\mathbf{I}_{1-M}$ and $\mathbf{I}_{1-N}$ respectively) are obtained most easily from the magnitude ratios $I_{2-M} = (0.188/0.385)I_f = 0.488I_f$ and $I_{2-N} = (0.214/0.385)I_f = 0.556I_f$ from which the corresponding current parallelogram is constructed. Likewise, the zero-sequence current components are obtained from the magnitude ratios $I_{0-M} = (0.188/0.632)I_f = 0.298I_f$ and $I_{0-N} = (0.489/0.632)I_f = 0.772I_f$.

The sequence voltages at M are found by adding the corresponding impedance drops to the sequence voltages at the point of fault. The positive-sequence voltage drop is $|\mathbf{I}_{1-M}|(0.097)$. This drop is added to $\mathbf{V}_1$ at the angle 34.0 degrees leading the current $\mathbf{I}_{1-M}$ to give the voltage $\mathbf{V}_{1-M}$. Since $\mathbf{I}_{1-M}$ is equal to $\mathbf{I}_{2-M}$, the same voltage drop in both magnitude and phase is subtracted from $-\mathbf{V}_2$ to give the voltage $-\mathbf{V}_{2-M}$. The zero-sequence voltage drop is $|\mathbf{I}_{0-M}|(0.238)$. This drop is subtracted from $-\mathbf{V}_0$ to give the voltage $-\mathbf{V}_{0-M}$.

Graphical Determination of the Direction of Relay Torque

The method for the graphical determination of the direction of relay torque is applicable to any case whether the sequence voltages and currents are obtained from the circle diagram, by calculation, or by use of the network analyzer. Once the vector diagram of the sequence quantities is constructed to scale, the direction of relay torque is obtained by graphical evaluation of the torque represented by the terms of the volt-ampere expressions of Table I for the relay and type of connection considered.

For the ordinary 60-degree connection, the relay most affected by a line-to-ground fault is relay c. The volt-ampere expression for this relay (see Table I) may be written $(\text{conj}\ \mathbf{V}_1)|\underline{60}(\mathbf{I}_1+\mathbf{I}_2+\mathbf{I}_0) - (\text{conj}\ \mathbf{V}_2)|\underline{120}(\mathbf{I}_1+\mathbf{I}_2+\mathbf{I}_0) - (\text{conj}\mathbf{V}_0)|\underline{0}\ (\mathbf{I}_1+\mathbf{I}_2+\mathbf{I}_0)$. Thus, the three sequence currents at the relay may be combined and the resultant treated as a common reference current for the determination of the behavior of the relay. With reference to Figure 9, the voltage $\mathbf{V}_{1-M}$ is rotated clockwise through 60 degrees and then projected on the current vector $(\mathbf{I}_1+\mathbf{I}_2+\mathbf{I}_0)_M$. Likewise, the voltage $-\mathbf{V}_{2-M}$ rotated clockwise through 120 degrees, and the voltage $-\mathbf{V}_{0-M}$ are projected on the same current vector. This process gives the three line segments OF, OG, and OH which are proportional in length to the components of relay torque represented respectively by the $\mathbf{V}_1$, $\mathbf{V}_2$, and $\mathbf{V}_0$ terms of the volt-ampere expression. Since the three line segments extend downward from O, each represents positive torque. The sum of the three is, therefore, positive, and correct relay operation is indicated.

The behavior of the relay at N is obtained by a similar process in which the current vector $(\mathbf{I}_1+\mathbf{I}_2+\mathbf{I}_0)_N$ and the voltages $\mathbf{V}_1$, $\mathbf{V}_2$, and $\mathbf{V}_0$ are used. The three components of torque are represented by the line segments Of, Og, and Oh. In this case, Oh represents negative torque. The resultant of all three however is positive, so that correct operation of the relay at N also is indicated.

Notation

$\mathbf{E}_{f0}$—Positive-sequence voltage at the point of fault before occurrence of the fault

$\mathbf{E}_{\alpha-f0}$, $\mathbf{E}_{\beta-f0}$—α and β components of the voltage at the point of fault before the occurrence of the fault

$\mathbf{I}_f$—Total fault component of current at the fault

$\mathbf{I}_1$, $\mathbf{I}_2$, $\mathbf{I}_0$—Positive-, negative-, and zero-sequence components of current at the relay, or in the fault if so specified

$\mathbf{I}_\alpha$, $\mathbf{I}_\beta$, $\mathbf{I}_0$—α, β, and zero components of current at the relay, or in the fault if so specified

R_1, R_2, R_0—Resistance components of $\mathbf{Z}_1$, $\mathbf{Z}_2$, and $\mathbf{Z}_0$

$\mathbf{V}_1$, $\mathbf{V}_2$, $\mathbf{V}_0$—Positive-, negative-, and zero-sequence components of voltage at the point of fault, or at the relay if so specified

conj $\mathbf{V}$—Conjugate of the voltage vector $\mathbf{V}$

X_1, X_2, X_0—Reactance components of $\mathbf{Z}_1$, $\mathbf{Z}_2$, and $\mathbf{Z}_0$

$\mathbf{Z}_1$, $\mathbf{Z}_2$, $\mathbf{Z}_0$—Total equivalent impedance of the positive-, negative-, and zero-sequence networks as measured at the point of fault

θ_1, θ_2, θ_0—Angles of the impedances $\mathbf{Z}_1$, $\mathbf{Z}_2$, and $\mathbf{Z}_0$

ϕ—The "relay angle"; that is, the angle between the current and voltage applied to the relay for which given magnitudes of these quantities cause maximum torque to be developed. The angle is positive if the current leads the voltage for this condition

When necessary to distinguish between components of current or voltage at different points in the network, subscripts based upon Figure 1b are used. Thus, $\mathbf{I}_{1-M}$ is the positive-sequence component of current at point M and $\mathbf{V}_{1-M}$ is the corresponding component of voltage.

Impedances of the different parts of the network are designated by the subscripts indicated in Figure 1b. Thus, $\mathbf{Z}_{1-Rf}$ is the impedance between the relay and point of fault in the positive-sequence network.

WYE AND DELTA CURRENTS AND VOLTAGES

$\mathbf{I}_A$, $\mathbf{I}_B$, $\mathbf{I}_C$—*Delta* currents

$\mathbf{I}_a$, $\mathbf{I}_b$, $\mathbf{I}_c$—Line or *wye* currents

$\mathbf{V}_A$, $\mathbf{V}_B$, $\mathbf{V}_C$—Line-to-line or *delta* voltages

$\mathbf{V}_a$, $\mathbf{V}_b$, $\mathbf{V}_c$—Line-to-neutral or *wye* voltages

$\mathbf{V}_A = \mathbf{V}_c - \mathbf{V}_b$ $\qquad \mathbf{I}_A = \mathbf{I}_c - \mathbf{I}_b$

$\mathbf{V}_B = \mathbf{V}_a - \mathbf{V}_c$ $\qquad \mathbf{I}_B = \mathbf{I}_a - \mathbf{I}_c$

$\mathbf{V}_C = \mathbf{V}_b - \mathbf{V}_a$ $\qquad \mathbf{I}_C = \mathbf{I}_b - \mathbf{I}_a$

References

1. A COMPREHENSIVE METHOD OF DETERMINING THE PERFORMANCE OF DISTANCE RELAYS, **J. H. Neher.** AIEE TRANSACTIONS, volume 56, 1937, July section, pages 833–44.

2. PROBLEMS SOLVED BY MODIFIED SYMMETRICAL COMPONENTS, **Edith L. Clarke.** *General Electric Review*, volume 41, 1938, pages 488–94, 545–9.

3. TWO-PHASE CO-ORDINATES OF A THREE-PHASE SYSTEM, **E. W. Kimbark.** AIEE TRANSACTIONS, volume 58, 1939, pages 894–904.

4. A SINGLE-ELEMENT POLYPHASE DIRECTIONAL RELAY, **A. J. McConnell.** AIEE TRANSACTIONS, volume 56, 1937, January section, pages 77–80.

5. Discussion by **E. W. Kimbark,** of reference 4, AIEE TRANSACTIONS, volume 56, 1937, August section, pages 1025–7.

6. FUNDAMENTAL BASIS FOR DISTANCE RELAYING, **W. A. Lewis, L. S. Tippett.** ELECTRICAL ENGINEERING, volume 50, June, 1931, pages 420–2.

7. DIRECTIONAL GROUND RELAYS, **E. E. George, R. H. Bennett.** AIEE TRANSACTIONS, volume 49, 1930, pages 1243–55.

8. RELAY SYSTEMS, **I. T. Monseth, P. H. Robinson.** McGraw-Hill Book Co., Inc., New York, N. Y.

9. National Electric Light Association Relay Handbook.

The Combination of Probability Curves in Engineering

ROGER I. WILKINSON
ASSOCIATE AIEE

1. Introduction

ENGINEERS are quite familiar with simple probability distributions showing the relative likelihoods of occurrence of the different values which a varying physical or electrical quantity may assume. As the complexities of machines and circuits in modern design increase, it becomes highly desirable to be able to estimate the probability distribution resulting from the joint presence of two or more sources of variation. This paper will undertake to present something of the theory of making such combinations, and its application to a number of practical examples.

2. Simple Probability Distributions

If a variable quantity x may take only values which are separated by finite intervals, it is known as a *discrete* variate. If there is no such restriction on the variable it is a *continuous* variate. We shall let p_x be the probability that the discrete variate takes the value x, and $\theta(r)dr$ be the probability that the continuous variate lies between r and $r+dr$.

To summarize in short compass the salient characteristics of any variate, discrete or continuous, it is convenient and customary to calculate the moments of its probability distribution. The ith moments taken about the zero of the variates are usually designated by μ_i', and are given by:

$$\left.\begin{array}{l} \textit{Discrete} \\ \mu_i' = \sum\limits_{x=\text{all values}} x^i p_x \\ \textit{Continuous} \\ \mu_i' = \int\limits_{r=\text{all values}} r^i \theta(r)dr \end{array}\right\} \quad (1)$$

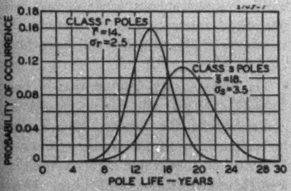


Figure 1. Two "mutually exclusive" universes

Likewise the ith moments taken about the average value ($\bar{x}$ or $\bar{r}$) of the variates are designated by μ_i, and are given by:

$$\left.\begin{array}{l} \mu_i = \sum\limits_{x=\text{all values}} (x-\bar{x})^i p_x \\ \mu_i = \int\limits_{r=\text{all values}} (r-\bar{r})^i \theta(r)dr \end{array}\right\} \quad (2)$$

Certain transfer formulas are available relating the μ_i''s and the μ_i's of the first four moments; they are often of value since it may be more convenient first to calculate from data or theory the moments about one of the two reference points (0 or the mean), and then obtain the other if desired. These formulas are:

$$\left.\begin{array}{l} \mu_0 = \mu_0' = 1 \\ \mu_1 = 0 \\ \mu_2 = \mu_2' - (\mu_1')^2 \\ \mu_3 = \mu_3' - 3\mu_2'\mu_1' + 2(\mu_1')^3 \\ \mu_4 = \mu_4' - 4\mu_3'\mu_1' + 6\mu_2'(\mu_1')^2 - 3(\mu_1')^4 \end{array}\right\} \quad (3)$$

A closely related set of descriptive functions which we shall want to use are known as semi-invariants. If the ith semi-invariant be designated λ_i, then

$$\left.\begin{array}{l} \lambda_0 = \mu_0' = \mu_0 = 1 \\ \lambda_1 = \mu_1' \\ \lambda_2 = \mu_2 \\ \lambda_3 = \mu_3 \\ \lambda_4 = \mu_4 - 3\mu_2^2 \end{array}\right\} \quad (4)$$

The 0th moment as seen by equations 1 is merely the sum of all values of the probability function and by definition must equal unity.

The first moment about zero, μ_1', of a variate is its *arithmetic mean* or average value $\bar{x}$ or $\bar{r}$; it thus indicates the general position of the variate along the scale of measurement.

The second moment μ_2 of a variate about its mean is a measure of the dispersion of the distribution. μ_2 is known as the *variance*, $\sigma=\sqrt{\mu_2}$ is called the *standard deviation*.

The third moment about the mean is a measure of the asymmetry of a variate's distribution; the asymmetry or *skewness* is defined as

$$k = \frac{\mu_3}{\mu_2^{3/2}} = \frac{\mu_3}{\sigma^3} \quad (5)$$

The fourth moment about the mean is a measure of the length of the tails and the peakedness of the mode of a distribution. This gives rise to the parameter known as the *excess* or *kurtosis* of a distribution; it is defined as

$$\beta_2 = \frac{\mu_4}{\mu_2^2} = \frac{\mu_4}{\sigma^4} \quad (6)$$

If the first few moments (or their associated parameters, mean, standard deviation, and so forth) serve to describe a probability distribution with considerable accuracy, then two distributions with a number of identical corresponding moments should closely resemble one another. To fit (or smooth) an observed probability distribution by an appropriate analytical curve form with unspecified constants, we first determine the lower moments of the analytical curve in terms of these constants. Then equating corresponding theoretical and observed moments gives relationships from which the constants of the fitting curve can be calculated. As many moments must be equated as there are unknown constants to be determined. For example, the value of the first moment or arithmetic mean a in the Poisson fitting distribution, $a^x e^{-a}/x!$ would be set equal to the observed mean of a set of data which this type of curve was appropriate to fit.

A variate's true distribution, if we could but determine it, would, except for rare instances, have a smooth unimodal form. Hence it will usually be desirable to smooth by the method of moments or in some other fashion a variate's rough observed distribution before entering upon its combination with other variates.

3. Laws of Combining Probabilities

Two or more events may occur independently, they may be mutually exclusive, or they may be correlated. If two events are independent, and the probability in favor of the first event is p_1, and in favor of the second is p_2, then the probability they will both occur is the *product* of their individual probabilities, p_1p_2. If either event a, or event b, or event c, and so forth, can happen, and the occurrence of one precludes the happening of another, they are known as mutually exclusive events. If events $a, b, c, d\ldots$ are mutually exclusive, the probability that *either a or b*, for instance, will occur is the *sum* of their individual probabilities,

---

Paper 42-145, recommended by the AIEE committee on communication for presentation at the AIEE summer convention, Chicago, Ill., June 22–26, 1942. Manuscript submitted April 29, 1942; made available for printing June 10, 1942.

Roger I. Wilkinson is a member of the technical staff, probability research department, Bell Telephone Laboratories, Inc., New York, N. Y.

$p_a + p_b$. Correlated events lie between the limiting cases of independent and mutually exclusive events, and the laws of combining their probabilities are determined by the amount and kind of dependence between them.

4. Combining Variates With Mutually Exclusive Probabilities

When two or more mutually exclusive variates are combined, the result is a "mingled" universe made up of variate r (with probability distribution p_r or $\theta(r)dr$) h proportion of the time, and variate s (with probability distribution q_s or $\phi(s)ds$) j proportion of the time.... The distribution functions of the resultant z of such mutually exclusive variates are:

$$\left.\begin{aligned}&\textit{Discrete Variates}\\ &F(z)=hp_z+jq_z+\ldots\\ &\textit{Continuous Variates}\\ &F(z)dz=h\theta(z)dz+j\phi(z)dz+\ldots\end{aligned}\right\}\qquad(7)$$

The moments of the resultant z taken about zero are readily expressed in terms of the moments of the component variates, as

$$\left.\begin{aligned}&\mu_{1z}'=h\mu_{1r}'+j\mu_{1s}'+\ldots\\ &\mu_{2z}'=h\mu_{2r}'+j\mu_{2s}'+\ldots\\ &\ldots\ldots\ldots\ldots\ldots\ldots\ldots\\ &\mu_{tz}'=h\mu_{tr}'+j\mu_{ts}'+\ldots\end{aligned}\right\}\qquad(8)$$

These equations permit us to express the parameters of z in terms of the parameters of its components as follows:

$$\left.\begin{aligned}&\bar{z}=h\bar{r}+j\bar{s}+\ldots\\ &\sigma_z=[h(\sigma_r^2+\bar{r}^2)+j(\sigma_s^2+\bar{s}^2)+\ldots\\ &\qquad\qquad-\bar{z}^2]^{1/2}\\ &k_z=\frac{1}{\sigma_z^3}\,[h(k_r\sigma_r^3+3\sigma_r^2\bar{r}+\bar{r}^3)+\\ &\quad j(k_s\sigma_s^3+3\sigma_s^2\bar{s}+\bar{s}^3)+\ldots-\\ &\qquad\qquad(3\sigma_z^2\bar{z}+\bar{z}^3)]\\ &\beta_{2z}=\frac{1}{\sigma_z^4}\,[h(\beta_{2r}\sigma_r^4+4k_r\sigma_r^3\bar{r}+6\sigma_r^2\bar{r}^2+\\ &\bar{r}^4)+j(\beta_{2s}\sigma_s^4+4k_s\sigma_s^3\bar{s}+6\sigma_s^2\bar{s}^2+\bar{s}^4)+\\ &\qquad\ldots-(4k_z\sigma_z^3\bar{z}+6\sigma_z^2\bar{z}^2+\bar{z}^4)]\end{aligned}\right\}\qquad(9)$$

Example—Length of Life of Outside Plant

A public utility serving a given area uses two kinds of poles in its outside plant. One kind, r, has an average life of 14 years with a standard deviation of 2.5 years, and the other, s, has an average life of 18 years with a standard deviation of 3.5 years. As shown in Figure 1 variations about the average life are substantially according to the normal law

$$f(x)=\frac{1}{\sigma_x\sqrt{2\pi}}\,e^{-\frac{(x-\bar{x})^2}{2\sigma_x^2}}$$

If approximately 20 per cent of the poles in the plant are of the r type, what is the distribution of life lengths on which the accounting department may base its pole depreciation rate?

Solution. The distribution of the overall life length will be, according to equation 7,

$$\begin{aligned}F(z)&=0.20\,\frac{1}{\sigma_r\sqrt{2\pi}}\,e^{-\frac{(z-\bar{r})^2}{2\sigma_r^2}}+\\ &\qquad 0.80\,\frac{1}{\sigma_s\sqrt{2\pi}}\,e^{-\frac{(z-\bar{s})^2}{2\sigma_s^2}}\\ &=\frac{0.20}{2.5\sqrt{2\pi}}\,e^{-\frac{(z-14)^2}{2(2.5)^2}}+\\ &\qquad \frac{0.80}{3.5\sqrt{2\pi}}\,e^{-\frac{(z-18)^2}{2(3.5)^2}}\end{aligned}$$

This distribution is shown in Figure 2 and is, of course, merely the weighted sum of the ordinates of the two mutually exclusive components. The parameters of this composite distribution are readily found from equations 9. For example, the average length of life of the whole pole plant is

$$\bar{z}=0.20(14)+0.80(18)=17.2\text{ years}$$

and the standard deviation of the universe of individual pole lives is

$$\sigma_z=[0.20(2.5^2+14^2)+0.80(3.5^2+18^2)-17.2^2]^{1/2}=3.69\text{ years}$$

5. Combining Independent Variates

In order to determine the resultant of any chosen values of two or more independent variates, the process by which they combine must be specified. This is expressed analytically by the *equation of relation;* it is written

$$z=\xi(r,s,t\ldots)\qquad(10)$$

For a particular choice of $r, s, t \ldots$ (denoted by subscripts) we have $z_1=\xi(r_1, s_1, t_1 \ldots)$. The probability of their joint occurrence is then, by section 3, the product of their individual probabilities of occurrence:

$$\left.\begin{aligned}&\textit{Discrete Variates}\\ &F(z_1)=p_rp_sp_t\ldots\\ &\textit{Continuous Variates}\\ &F(z_1)dz=\theta(r_1)dr\phi(s_1)ds\psi(t_1)dt\ldots\end{aligned}\right\}\qquad(11)$$

Equations 11 give the probability for obtaining z by just one particular selection from usually a number of mutually exclusive combinations of $r, s, t \ldots$ which when substituted in equations 11 will produce the given value of $z=z_1$. Hence by the law of combining mutually exclusive events, the total probabilities of obtaining z_1 are

$$\left.\begin{aligned}&\textit{Discrete Variates}\\ &F(z_1)=\mathbb{S}p_rp_sp_t\ldots\\ &\textit{Continuous Variates}\\ &F(z_1)dz=\mathbb{S}\theta(r)dr\phi(s)ds\psi(t)dt\ldots\end{aligned}\right\}\qquad(12)$$

where the symbol $\mathbb{S}$ indicates the summation of all eligible cases. Since z_1 is any value of z, equations 12 are the desired expressions for the probability distribution of z.

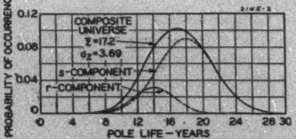


Figure 2. Composite universe formed from two "mutually exclusive" universes

5.1 Step-by-Step Combination of Independent Variates

It is clearly possible to obtain a distribution of the resultant $F(z)$, by writing out in tabular form all the ways of forming each value of z, then taking the products of the individual probabilities of occurrence for each way as in equations 11, and finally summing the probabilities according to equations 12. Such a procedure does not give the result in a general or analytical form for use in subsequent applications, but it does have the immediate advantage of providing the solution to a particular problem.

When more than two elemental variates are to be combined, it is nearly always possible first to combine two, then combine their resultant with the third elemental variate, and so on. The combination of any two variates is then done by means of a two-way or "square" table displaying the values of one variate (say s) with their corresponding probabilities of occurrence in the horizontal direction as column headings, and the values and probabilities of the other variate (say r) in the vertical direction, as line headings. An outline of this form is shown in Table I. There is no theoretical need that the range of the variate r represented by r_1 should equal the next range r_2, and the subsequent r ranges, or that the r ranges should be the same as the s ranges. Practically, however, such a selection is usually made.

For discrete variates the unit difference between successive r's and s's will generally be just one unit on the natural scale of measurement, such as one call, one object, one occurrence; otherwise the exactness of the solution will have been lost.

The step-by-step combination of continuous variates will seldom be wholly exact, because the variates' ranges must be broken down into a number of discrete intervals, each of which is represented by a single more or less typical value. However by taking these intervals smaller and

Table I

		s—Variate				
		s_1 $\phi(s_1)$	s_2 $\phi(s_2)$	s_3 $\phi(s_3)$	—	s_n $\phi(s_n)$
r—variate r_1	$\theta(r_1)$	$\xi(r_1, s_1)=z_{11}$ $F(z_{11})=\theta(r_1)\cdot\phi(s_1)$	$\xi(r_1, s_2)=z_{12}$ $F(z_{12})=\theta(r_1)\cdot\phi(s_2)$	$\xi(r_1, s_3)=z_{13}$ $F(z_{13})=\theta(r_1)\cdot\phi(s_3)$		$\xi(r_1 s_n)=z_{1n}$ $F(z_{1n})=\theta(r_1)\cdot\phi(s_n)$
r_2	$\theta(r_2)$	$\xi(r_2, s_1)=z_{12}$ $F(z_{21})=\theta(r_2)\cdot\phi(s_1)$	etc. etc.		. . .	
r_3	$\theta(r_3)$	$\xi(r_3, s_1)=z_{31}$ $F(z_{31})=\theta(r_3)\cdot\phi(s_1)$			. . .	
. . .	. . .	. . .	. . .	. . .	. . . . .	. . .
r_m	$\theta(r_m)$	$\xi(r_m, s_1)=z_{m1}$ $F(z_m)=\theta(r_m)\cdot\phi(s_1)$			. . .	$\xi(r_m, s_n)=z_{mn}$ $F(z_{mn})=\theta(r_m)\cdot\phi(s_n)$

smaller, the errors in the resultant may be reduced to any required amount.

In the small areas opposite each pair of r_i, s_j values in Table I are shown two entries. In the upper left corner is the value of z_{ij} which results from substituting r_i and s_j in the equation of relation $z_{ij}=\xi(r_i, s_j)$; it is, of course, the value of z which would occur if these particular values of r and s were present. In the lower right corner is entered the product of the two probabilities shown at the column and line headings, that is $\theta(r_i)\times\phi(s_j)$. Thus upon the completion of the table, all values of r have been combined according to the equation of relation with all values of s. If any restrictions are to be observed on the association of certain ranges of r with those of s, the proper areas are readily omitted from the table. If m classes of r and n classes of s are made, then we shall have mn z values each with its associated probability of occurrence.

The distribution $F(z)$ may now be formed in a manner identical with that used in grouping observed data, that is, class intervals are chosen, the proper values of z are assigned to each, and the probability of occurrence in each interval obtained by adding the probabilities for all of the z's falling therein. The cumulative distribution and the moments and parameters of z may then be calculated in the usual way if desired.

If the simple sum or difference of the r and s variates is to be found, dividing each into intervals of the same width as suggested above will expedite the working. In Table I all the values of a particular $z_w=r+s$ for the sum case (or $r-s$ for the difference case) will lie along one diagonal. The values of z_{w-1} will lie on the next diagonal above; the values of z_{w+1} on the next diagonal below and so on. Hence

1. There is no need to write in the z values in the body of the table.
2. The sum of the probabilities shown in any z diagonal is the total probability of occurrence of that value of z.

Each such sum may then be written conveniently at the edge of the square table opposite the end of its diagonal; this is the desired probability distribution.

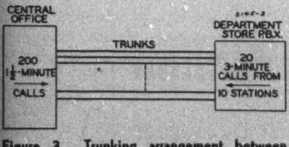


Figure 3. Trunking arrangement between PBX and central office

Example—PBX Trunk Engineering

A group of trunks is to be provided for two-way use between a department store PBX (private branch exchange) as in Figure 3. There will be ten "outgoing" telephones at the store, originating on the average two three-minute trunk calls each in the busy hour. In addition, a sufficient number of stations will be provided at the store for incoming service only, to handle the 200 1½ minute customers' calls which are made in the average busy hour. How many trunks should be installed so that the customers may receive $P=0.01$ service?

Solution. Each of the 10 outgoing telephones will have a probability p of $\frac{2(3)}{60}=0.10$ of being in use at any random instant. The probability that exactly s will be so engaged is the binomial term

$$p_s=\phi(s)=\frac{n!}{s!(n-s)!}p^s(1-p)^{n-s}$$

where $n=10$ and $p=0.10$. Since a large number of sources will originate the incoming calls their distribution will be well represented by the Poisson expression

$$q_r=\theta(r)=\frac{a^r e^{-a}}{r!}$$

where

a = average simultaneous calls

$$=\frac{200(1^1/_2)}{60}=5.0$$

We now construct the square Table II, first calculating the various values of $\theta(r)$ and $\phi(s)$ from the above formulas to insert as column and line headings respectively. The s values have been reversed from the usual ascending tabular order so that the constant z diagonals will end at the right-hand edge of the table. The product of each pair of probabilities $\theta(r_i)\phi(s_j)$ is recorded in the body of the table and the diagonals summed as shown to give the desired $F(z)$ distribution. The cumulation of $F(z)$, designated $P(\geqq z)$, is also given.

From this last column we read the answer desired: To insure that the customers will not find the lines to the store busy oftener than 0.01 of the time, we must provide 13 trunks $[P(\geqq 13)=0.0083]$.

5.2 The Exact Analytical Combination of Independent Variates

If the probability distributions represented by the symbols p_r, p_s…and $\theta(r)dr$, $\phi(s)ds$ …, in equations 12 are expressed as mathematic functions, it may well be that in numerous cases the summation operation indicated by $\mathfrak{S}$ can be performed analytically, thus giving the resultant $F(z)$ in similar analytical form. We must remember that $\mathfrak{S}$ represents a very special summation in which the relationships expressed by the equation of relation 10 are maintained. We may insure this by solving for one of the variates, say r, in equation 10, obtaining

$$\left.\begin{aligned} &r=f(z, s, t\ldots) \\ &\text{and} \\ &dr=\frac{df(z, s, t\ldots)}{dz}dz \end{aligned}\right\} \quad (13)$$

and substituting these in equations 12

Then the remaining independent variates may be summed over all ranges consistent with the value of z selected. Equations 12 then assume the completely definitized forms:

Discrete Variates

$$F(z)=\sum_s\sum_t \ldots \sum p_{f(z,\,s,\,t)} \ldots p_s p_t \ldots$$

Continuous Variates

$$\left.\begin{aligned} &F(z)dz= \\ &\quad dz\int_s\int_t \ldots \int \theta[f(z,\,s,\,t\ldots)]\times \\ &\qquad \frac{d[f(z,\,s,\,t\ldots)]}{dz}\phi(s)ds\psi(t)dt\ldots \end{aligned}\right\} \quad (14)$$

5.21 Analytical Addition of Discrete Variates

The basic equation 14 for discrete variates can of course be applied whatever the equation of relation, and often its straightforward use is the simplest procedure. However for the addition, which is by far the commonest case, of discrete variates a method involving *frequency arrays* is found to be very powerful.

For our purpose, a frequency array is an expression containing the index A, which when *expanded in powers of* A, will give as the coefficient of A^r, the probability of occurrence of exactly r successes or events. Thus for the Poisson distribution the frequency array is $e^{m(A-1)}$, since, when expanded in powers of A, it yields the individual Poisson terms

$$e^{m(A-1)}=e^{-m}\left(1+mA+\frac{m^2}{2!}A^2+\frac{m^3}{3!}A^3+\ldots+\frac{m^r}{r!}A^r+\ldots\right)$$

We note that the probability of finding exactly r events occurring with an average occurrence of m is the coefficient of A^r, that is $m^re^{-m}/r!$

Secondly by substituting e^α for A, we obtain the "moment array" of the variate, which possesses the property that when it is expanded in *powers of* α, the successive moments of the distribution taken about zero are given as the coefficients of $\alpha, \frac{\alpha^2}{2!}, \frac{\alpha^3}{3!} \ldots$. Moreover the moment array *with respect to the mean* instead of the origin of the variate can be written by adding the factor $e^{-\alpha(\text{mean})}$. Thus the Poisson moment array with respect to the mean would be $e^{-\alpha m}\times e^{m(e^\alpha-1)}$. Expanding we find

$$e^{m(e^\alpha-1-\alpha)}=1+m\frac{\alpha^2}{2!}+m\frac{\alpha^3}{3!}+(m+3m^2)\frac{\alpha^4}{4!}+\ldots$$

Table II. Step-by-Step Addition of Binomial and Poisson Variates

Number of Calls in Progress by Customers (Average = 5.0) $\theta(r)=\frac{a^re^{-a}}{r!}$ r	$\theta(r)$	Number of Calls in Progress From Store Binomial: $\phi(s)=\{{n \atop s}\}p^s(1-p)^{n-s}$; n = 10, p = 0.1 (Average = 1.0)											Total Calls in Progress From Both Sources		
		s = 10 $\phi(s)$ = —	s = 9 $10^{-9}\cdot 9.000$	s = 8 $10^{-7}\cdot 3.64501$	s = 7 $10^{-6}\cdot 8.73803$	s = 6 0.000138	s = 5 0.001488	s = 4 0.011160	s = 3 0.057396	s = 2 0.193710	s = 1 0.387420	s = 0 0.348678	z = r + s	F(z)	P(≦z)
0	0.006738					0.000001	0.000010	0.000075	0.000387	0.001305	0.002610	0.002349	0	0.002349	0.999992
1	0.033690					0.000005	0.000050	0.000376	0.001934	0.006526	0.013052	0.011747	1	0.014357	0.997643
2	0.084224				0.000001	0.000012	0.000125	0.000940	0.004834	0.016315	0.032630	0.029367	2	0.043724	0.983286
3	0.140374				0.000001	0.000019	0.000209	0.001567	0.008057	0.027192	0.054384	0.048945	3	0.088488	0.939562
4	0.175467				0.000002	0.000024	0.000261	0.001958	0.010071	0.033990	0.067979	0.061182	4	0.133890	0.851074
5	0.175467				0.000002	0.000024	0.000261	0.001958	0.010071	0.033990	0.067979	0.061182	5	0.161573	0.717184
6	0.146223				0.000001	0.000020	0.000218	0.001632	0.008392	0.028325	0.056650	0.050985	6	0.162002	0.555611
7	0.104445				0.000001	0.000014	0.000155	0.001166	0.005995	0.020232	0.040464	0.036418	7	0.138826	0.393609
8	0.065278				0.000001	0.000009	0.000097	0.000728	0.003747	0.012645	0.025290	0.022761	8	0.103800	0.254783
9	0.036266					0.000005	0.000054	0.000405	0.002082	0.007025	0.014050	0.012645	9	0.068798	0.150983
10	0.018133					0.000002	0.000027	0.000202	0.001041	0.003512	0.007025	0.006322	10	0.040930	0.082185
11	0.008242					0.000001	0.000012	0.000092	0.000473	0.001596	0.003193	0.002874	11	0.022081	0.041255
12	0.003434						0.000005	0.000038	0.000197	0.000665	0.001330	0.001197	12	0.010889	0.019174
13	0.001321						0.000002	0.000015	0.000076	0.000256	0.000512	0.000461	13	0.004945	0.008285
14	0.000472						0.000001	0.000005	0.000027	0.000091	0.000183	0.000164	14	0.002080	0.003340
15	0.000157							0.000002	0.000009	0.000030	0.000061	0.000055	15	0.000816	0.001260
16	0.000049							0.000001	0.000003	0.000009	0.000019	0.000017	16	0.000297	0.000444
17	0.000014								0.000001	0.000003	0.000005	0.000005	17	0.000102	0.000147
18	0.000004									0.000001	0.000002	0.000001	18	0.000031	0.000045
19	0.000001									0.000000	0.000000	0.000000	19	0.000011	0.000014
													20	0.000003	0.000003

Hence the first, second, third, fourth ... moments of the Poisson variate about its mean are 0, m, m, $m+3m^2$ ... respectively. In Table III are given for four discrete variates common in engineering, the expressions for the frequency and moment arrays and the corresponding parameters.

The important characteristic of frequency arrays which we can make use of in adding discrete variates is that the array of their sum is the *product* of the individual arrays. Thus to add two Poisson variates r and s with distributions p_r and p_s, we multiply their frequency arrays giving

Array of $z=r+s$ is $[e^{a_r(A-1)}][e^{a_s(A-1)}]$

This when expanded in powers of A gives

$$e^{-(a_r+a_s)}e^{A(a_r+a_s)}=e^{-(a_r+a_s)}\times\left(1+(a_r+a_s)A+\frac{(a_r+a_s)^2}{2!}A^2+\frac{(a_r+a_s)^3}{3!}A^3+\ldots\right)$$

But by definition the coefficient of A^z is the probability of exactly z occurrences. Hence

$$F(z)=\frac{(a_r+a_s)^z e^{-(a_r+a_s)}}{z!}$$

which demonstrates the truth of the familiar statement that the sum of two Poisson variates is a third Poisson variate whose average equals the sum of the components' averages. This same result could have been found by direct application of equation 14.

The sum distribution of the binomial and Poisson variates found step-by-step in Table II can readily be checked by the frequency array method. In a similar fashion the analytical expression for the sum and the moments of the sum of any two or more discrete variates can be obtained. The individual variates, whose frequency arrays are given in Table III will be found to simulate quite a number of the simple discrete distributions found in practice. The table could, of course, be extended indefinitely.

5.22 Analytical Combination of Continuous Variates

The application of equation 14 to the three most common equations of relation between two continuous variates yields

Linear Combination, $z=r\pm s$

$$F(z)dz=dz\int_s \theta(z\mp s)\phi(s)ds \qquad (15)$$

Product Combination, $z=rs$

$$F(z)dz=dz\int_s \theta\left(\frac{z}{s}\right)\frac{1}{s}\phi(s)ds \qquad (16)$$

Quotient Combination, $z=\frac{r}{s}$

$$F(z)dz=dz\int_s \theta(zs)s\phi(s)ds \qquad (17)$$

The combinations of a great variety of continuous variates even in just the three type groups above can be imagined. We shall take up a few cases which illustrate the method of working.

5.22.11 Linear Combination of Distributions Limited in Both Directions—Square-Topped

If we have two square-topped variates r and s with distributions

$$\theta(r)dr=\frac{1}{b-a}dr$$

$$\phi(s)ds=\frac{1}{h-g}ds$$

what is the distribution of z, when $z=r+s$? Consider first the simple case of $a>h$ and $h-g>b-a$ (or $h+a>b+g$), as shown in Figure 4. From equation 15 we write immediately,

$$F(z)dz=\frac{dz}{b-a}\frac{1}{h-g}\int_{s=\text{all values}} ds$$

We need now to select carefully the limits of integration for s which include all the admissible values of s but no others. There will be three cases.

Case 1. When z lies between $a+g$ and $b+g$

$$F_1(z)=\frac{1}{b-a}\frac{1}{h-g}\int_{s=g}^{z-a} ds$$

$$=\frac{1}{b-a}\frac{1}{h-g}(z-a-g)$$

Case 2. When z lies between $b+g$ and $a+h$

$$F_2(z)=\frac{1}{b-a}\frac{1}{h-g}\int_{s=z-b}^{z-a} ds$$

$$=\frac{1}{b-a}\frac{1}{h-g}(b-a)=\frac{1}{h-g}$$

Case 3. When z lies between $a+h$ and $b+h$

$$F_3(z)=\frac{1}{b-a}\frac{1}{h-g}\int_{s=z-b}^{h} ds$$

$$=\frac{1}{b-a}\frac{1}{h-g}(b+h-z)$$

The distribution of the resultant z is shown in Figure 4, and, as one might have expected, shows discontinuities at the points $b+g$ and $a+h$. Clearly the restriction of $a>h$ would make no change if it were lifted. We insure (although not absolutely) our selection of the proper limits for s by checking that the area under $F(z)$ is unity, that is

$$\text{Area}=\int_{a+g}^{b+g}F_1(z)dz+\int_{b+g}^{a+h}F_2(z)dz+\int_{a+h}^{b+h}F_3(z)dz=1$$

If instead of $h+a>b+g$ we had taken $h+a<b+g$, the equations for $F_1(z)$ and $F_3(z)$ would remain unchanged for the end intervals, while $F_2(z)$ for the central interval of z would become

$$F_2'(z)dz=\frac{1}{b-a}\frac{1}{h-g}\int_{s=g}^{h} ds$$

$$=\frac{1}{b-a}\frac{1}{h-g}(h-g)=\frac{1}{b-a}$$

Finally it may be observed that when $a+h=b+g$, that is the two variates r and s have the same range, the central interval of z disappears entirely, leaving simply an isosceles triangular distribution whose average is $\bar{z}=\bar{r}+\bar{s}$, and whose range is the sum of the corresponding ranges of the r and s variates.

The distribution of the *difference* between two square-topped variates is obtained in a manner very similar to that used in determining their sum. Likewise, the distribution form resulting is very similar to the sum distribution; an example is shown as the lower diagram of Figure 4.

EXAMPLE—MINIMIZING NOISE IN TELEPHONE CIRCUITS

Series capacitors are used in each side of the talking circuit at several points in local crossbar central office practice to isolate

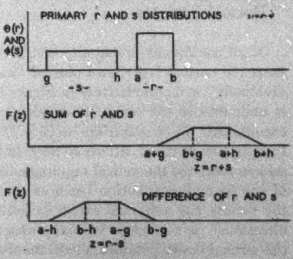


Figure 4. Linear combination of "square-topped" distributions

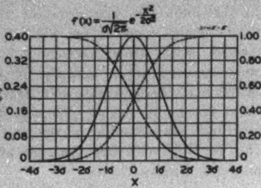


Figure 5. Individual and cumulative normal distribution

various d-c circuit elements. In order to insure satisfactory longitudinal unbalance characteristics (to mimimize noise) the two capacitors comprising a pair need to have very nearly identical capacitances. Suppose stock capacitors available for this purpose vary in capacitance with equal likelihood over the range 2.2 microfarads ±2.5 per cent. What will be the expected (or average) unbalance in the two sides of the circuit due to this cause? What is the probability the unbalance will exceed 0.08 microfarad?

Solution. Since the two capacitor capacitance distributions are identical, the $F(z)$ distribution will be an isosceles triangle, ranging about the mean $\bar{z}=0$, from $-2(0.025)(2.2)=-0.11$ microfarad to $+0.11$ microfarad. The expected unbalance will be found by determining the average of the absolute deviations from $\bar{z}=0$. In the notation used above and seen on Figure 4, this is

$$\overline{|z|}=2\int_0^{b-g}\frac{z}{(b-a)(h-g)}(b-g-z)dz$$

$$=0.0367 \text{ microfarad}$$

when $b=h=2.2+0.055=2.255$, and $a=g=2.2-0.055=2.145$. Similarly the probability that the unbalance z will exceed 0.08 microfarad is given by

$$P(>0.08)=\int_{0.08}^{0.11}F(|z|)dz=\frac{2}{(b-a)(h-g)}\times$$

$$\int_{0.08}^{0.11}(b-g-z)dz=0.0744$$

5.22.12 Linear Combination of Normal Variates

Of all continuous probability distributions the normal law shown plotted "individually" and cumulatively in Figure 5 is undoubtedly the one most frequently encountered in engineering work. Although the physical situation may but seldom meet the theoretical requirements of the normal distribution (such as that the variate has unlimited extent in both directions), nevertheless in many cases the normal curve provides a satisfactory fit over the major portion of the variate's range.

Let the variates r and s have the normal distributions

$$\theta(r)=\frac{1}{\sigma_r\sqrt{2\pi}}e^{-\frac{(r-\bar{r})^2}{2\sigma_r^2}} \tag{18}$$

and

$$\phi(s)=\frac{1}{\sigma_s\sqrt{2\pi}}e^{-\frac{(s-\bar{s})^2}{2\sigma_s^2}} \tag{19}$$

We desire the distribution of $z=r+s$. Substituting directly in equation 15 gives

$$F(z)dz=dz\int_{s=-\infty}^{+\infty}\theta(z-s)\phi(s)ds$$

$$=dz\int_{-\infty}^{\infty}\frac{1}{\sigma_r\sqrt{2\pi}}e^{-\frac{(z-s-\bar{r})^2}{2\sigma_r^2}}\times$$

$$\frac{1}{\sigma_s\sqrt{2\pi}}e^{-\frac{(s-\bar{s})^2}{2\sigma_s^2}}ds$$

$$=\frac{1}{\sqrt{\sigma_r^2+\sigma_s^2}\sqrt{2\pi}}e^{-\frac{[z-(\bar{r}+\bar{s})]^2}{2(\sigma_r^2+\sigma_s^2)}}dz \tag{20}$$

Quite obviously this resultant distribution is of the same normal form as were the two components, since if we set

$$\bar{z}=\bar{r}+\bar{s} \tag{21}$$

$$\sigma_z=\sqrt{\sigma_r^2+\sigma_s^2} \tag{22}$$

we obtain

$$F(z)dz=\frac{1}{\sigma_z\sqrt{2\pi}}e^{-\frac{(z-\bar{z})^2}{2\sigma_z^2}} \tag{23}$$

By an exactly similar analysis we should have found that the distribution of the *difference* $z=r-s$ of two normal variates has exactly the same distribution as given in equation 23, if instead of equation 21 we write

$$\bar{z}=\bar{r}-\bar{s} \tag{24}$$

It is thus quite obvious that *the sum (or difference) of two normal variates has a normal distribution whose mean equals the sum (or difference) of the means of the primary distributions, and whose standard deviation is the square root of the sum of the squares of the component standard deviations.*

EXAMPLE—THE TRACTOR PLANT

In the assembly plant of a tractor company, one operation consists in bolting two connecting parts A and B end to end as in Figure 6. It is found by sampling the bins containing the two parts that eight per cent of the A parts exceed a variation of ±0.10 inch from the nominal or design value, and 14 per cent of the B parts exceed a variation of ±0.05 inch from their nominal value. What proportion of the bolted assemblies will exceed an error of 0.15 inch from the designed value?

Solution. Suppose past studies have shown that both parts A and B have manufacturing variations closely following the normal law. Then reading on Figure 5, we see that if eight per cent are to exceed a deviation from the mean of ±0.10 inch (that is four per cent exceed a +0.10-inch deviation and four per cent exceed a −0.10-inch deviation), the +0.10 deviation must correspond to +1.75 σ_A standard deviations, whence $\sigma_A=\frac{0.10}{1.75}=0.05714$ inch. Likewise with part B if we find that seven per cent of the samples exceed a deviation of +0.05 then 0.05 equals 1.48 standard deviations, or $\sigma_B=\frac{0.05}{1.48}=0.03378$ inch.

Now from equation 22 we calculate the standard deviation of the sum of the lengths of A and B, as

$$\sigma_z=\sqrt{(0.05714)^2+(0.03378)^2}=0.06638$$

We know from equation 23 that z is normal, so we have merely to find the probability of exceeding a deviation from the average of the specified 0.15 inch in a normal curve with $\sigma_z=0.06638$. This deviation when expressed in terms of σ_z is 2.26 standard deviations. Referring once more to Figure 5 we find the probability is approximately $2(0.012)=0.024$ that a random assembled unit will exceed the specified range. Or, conversely, we should expect $(1-0.024)100=97.6$ per cent of the assemblies to fall within the specified range.

5.22.2 Product Combinations of Variates

A single pair of variates will be chosen to illustrate the procedure followed to obtain the distribution of their product. Suppose we have a generalized exponential distribution (often designated as Pearson's type III) in r and a square-topped distribution in s, given by

$$\theta(r)=\frac{c^{n+1}}{\Gamma(n+1)}r^ne^{-cr},$$

$$\phi(s)=\frac{1}{h-g}$$

to find $F(z)$, when $z=rs$.

Substituting in equation 16 gives

$$F(z)dz=dz\int\theta\left(\frac{z}{s}\right)\frac{1}{s}\phi(s)ds$$

$$=dz\frac{c^{n+1}}{\Gamma(n+1)}\frac{1}{h-g}\int_{s=g}^{h}\left(\frac{z}{s}\right)^n e^{-c\frac{z}{s}}\frac{1}{s}ds$$

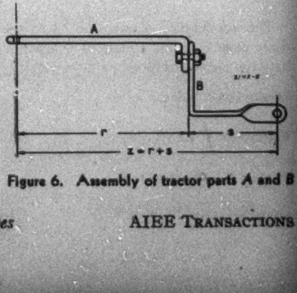


Figure 6. Assembly of tractor parts *A* and *B*

Table III. The Frequency and Moment Arrays, and Parameters of Several Common Discrete Distributions

		Moment Arrays		Parameters of the Universe			
Universe	Frequency Array	About Zero*	About Mean**	mean, $\bar{x}$	Standard Deviation, σ	Skewness, $k(=\sqrt{\beta_1})$	Kurtosis, β_2
Binomial:							
$f(x)=\binom{n}{x}p^x(1-p)^{n-x}$	$(q+pA)^n$	$(q+pe^{\alpha})^n$	$e^{-np\alpha}(q+pe^{\alpha})^n$	np	$\sqrt{npq}$	$\frac{1-2p}{\sqrt{npq}}$	$3\frac{n-2}{n}+\frac{1}{npq}$
Poisson:							
$f(x)=\frac{m^x e^{-m}}{x!}$	$e^{m(A-1)}$	$e^{m(e^{\alpha}-1)}$	$e^{-m\alpha}\cdot e^{m(e^{\alpha}-1)}$	m	$\sqrt{m}$	$\frac{1}{\sqrt{m}}$	$3+\frac{1}{m}$
Simple exponential:							
$f(x)=(1-e^{-c})e^{-cx}$	$\frac{1-e^{-c}}{1-Ae^{-c}}$	$\frac{1-e^{-c}}{1-e^{\alpha-c}}$	$e^{\frac{\alpha e^{-c}}{1-e^{-c}}}\frac{(1-e^{-c})}{(1-e^{\alpha-c})}$	$\frac{e^{-c}}{1-e^{-c}}$	$\frac{e^{-\frac{c}{2}}}{1-e^{-c}}$	$e^{\frac{c}{2}}+e^{-\frac{c}{2}}$	$e^{c}+e^{-c}+7$
Generalized exponential:							
$f(x)=\frac{(1-e^{-c})^2}{e^{-c}}xe^{-cx}$	$A\frac{(1-e^{-c})^2}{(1-Ae^{-c})}$	$e^{\alpha}\frac{(1-e^{-c})^2}{(1-e^{\alpha-c})}$	$e^{\frac{2\alpha}{1-e^{-c}}}\frac{(1-e^{-c})^2}{(1-e^{\alpha-c})}$	$\frac{1+e^{-c}}{1-e^{-c}}$	$\frac{\sqrt{2}e^{-\frac{c}{2}}}{1-e^{-c}}$	$\frac{e^{\frac{c}{2}}+e^{-\frac{c}{2}}}{\sqrt{2}}$	$\frac{e^{c}+e^{-c}+10}{2}$

* The moment array about zero is obtained by substituting $e\alpha$ for A in the frequency array.

** The moment array about the mean is obtained by multiplying the moment array about zero by $e^{-\alpha(\text{mean})}$.

Let $t=\frac{1}{s}$, so that $s=\frac{1}{t}$, $ds=-\frac{}{t^2}dt$. This results in

$$F(z)dz=\frac{c^{n+1}}{\Gamma(n+1)}\frac{z^n dz}{h-g}\times \int_{1/h}^{1/g} t^{n-1}e^{-czt}dt \qquad (25)$$

The integration of this formula for n unspecified cannot be performed exactly. For low integral values of n, however, the expression can readily be written out by making a succession of n integrations, since

$$\int t^{n-1}e^{-ct}dt=\frac{t^{n-1}e^{-ct}}{-c}+\frac{n-1}{c}\int t^{n-2}e^{-ct}dt$$

until the last integral is of the form

$$\int e^{-ct}dt=-\frac{1}{c}e^{-ct}$$

Thus when $n=1$, equation 25 becomes simply

$$F(z)dz=\frac{c}{\Gamma(2)}\frac{dz}{h-g}\left[e^{-\frac{cz}{h}}-e^{-\frac{cz}{g}}\right]$$

If n is any positive integer, it can be shown that

$$F(z)dz=\frac{c}{n}\frac{dz}{h-g}\left[P\left(n,\frac{cz}{g}\right)-P\left(n,\frac{cz}{h}\right)\right]$$

where $P(n, m)=\sum_{x=n}^{\infty}\frac{m^x e^{-m}}{x!}$, the Poisson summation which is tabled for wide ranges.

5.22.3 Quotient Combination of Variates

To illustrate the exact analytical determination of the distribution of the quotient of two independent variates, we choose the case of $z=r/s$ in which r and s are of the generalized exponential form. Their distributions are

$$\theta(r)=\frac{c^{n+1}}{\Gamma(n+1)}r^n e^{-cr}$$

$$\phi(s)=\frac{q^{m+1}}{\Gamma(m+1)}s^m e^{-qs}$$

From equation 17

$$\begin{aligned}F(z)dz&=dz\int\theta(zs)s\phi(s)ds\\&=dz\frac{c^{n+1}}{\Gamma(n+1)}\frac{q^{m+1}}{\Gamma(m+1)}\times\int_{s=0}^{\infty}(zs)^n e^{-czs}s^{m+1}e^{-qs}ds\\&=\frac{c^{n+1}}{\Gamma(n+1)}\frac{q^{m+1}}{\Gamma(m+1)}z^n dz\times\int_{s=0}^{\infty}s^{m+n+1}e^{-(cz+q)s}ds \qquad (26)\end{aligned}$$

Apparently equation 26 is of the same form as equation 25 just considered under "Products," except that here the limits of s are 0 and ∞. This last condition produces the complete gamma function, since

$$\int_0^{\infty}x^n e^{-ax}dx=\frac{\Gamma(n+1)}{a^{n+1}}$$

in which $\Gamma(n+1)=n!$ for n integral. Hence equation 26 reduces to

$$F(z)dz=c^{n+1}q^{m+1}\frac{\Gamma(m+n+2)}{\Gamma(n+1)\Gamma(m+1)}\times\frac{z^n dz}{(cz+q)^{m+n+2}} \qquad (26\text{-a})$$

Tables of logarithms of the Γ function are available as well as factorials of numbers which may be used in their place when m and n are integers.

Since the rth moment of z in equation 26 is

$$\mu_r'=\frac{q^r}{c^r}\frac{\Gamma(n+r+1)}{\Gamma(n+1)}\frac{\Gamma(m-r+1)}{\Gamma(m+1)}$$

we readily find

$$\bar{z}=\frac{q}{c}\frac{n+1}{m}$$

and

$$\sigma_z=\frac{q}{cm}\sqrt{\frac{(m+n+1)(n+1)}{m-1}}$$

It is to be particularly noted that $\bar{z}$ does *not* equal the ratio of $\bar{r}$ and $\bar{s}$, which here would give $\frac{q}{c}\frac{n+1}{m+1}$ instead of $\frac{q}{c}\frac{n+1}{m}$.

5.3 Approximate Analytical Combinations of Independent Variates

Very often it becomes highly desirable to find an estimate of the resultant of two or more independent variates when it is impossible or inexpedient to combine them either by the step-by-step or the completely analytical method. Sometimes, for instance, raw data depicting the behavior of one of the elements are widely scattered or are not easily fitted by the usual methods; a fitting curve may have too complex an equation to permit analytical manipulation; complete information on one or more primary variates may be lacking; or again only estimates of the resultant parameters may be needed for one's purpose.

In this event satisfactory resort may usually be made to various schemes for combining the parameters of the distribution functions to obtain the corresponding parameters of the resultant curve. From these, in turn, a frequency distribution may be constructed if desired, using one of the systems of generalized frequency curves (such as the Pearson types or the Gram-Charlier series). The more powerful of these combination methods will now be considered for the three common operations, those having linear, product, and quotient equations of relation.

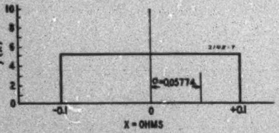


Figure 7. Resistance variation in coils of wire

5.31 Approximate Linear Combinations

The parameters which lend themselves best to the approximate linear combination of variates are the semi-invariants defined by equations 4. Thiele has demonstrated the following relationship for every linear function of uncorrelated observations, the extreme simplicity of which he says "renders the semi-invariants unrivalled as the most suitable symmetrical functions and the most powerful instrument of the theory of observations."

If the equation of relation is $z=a+bx+cy+\ldots+dt$ then the semi-invariants L_r of the resultant z distribution are

$$\left.\begin{array}{l} L_1=a+b\,{}_x\lambda_1+c\,{}_y\lambda_1+\ldots+d\,{}_t\lambda_1 \\ L_2=b^2\,{}_x\lambda_2+c^2\,{}_y\lambda_2+\ldots+d^2\,{}_t\lambda_2 \\ \cdots\cdots\cdots\cdots\cdots\cdots \\ \text{and, generally} \\ L_r=b^r\,{}_x\lambda_r+c^r\,{}_y\lambda_r+\ldots+d^r\,{}_t\lambda_r \end{array}\right\} \qquad (27)$$

for $r \geqq 2$.

Having found the semi-invariants of the combined distributions, we may if we like, retransfer them to moments by equations 4, and then determine the mean standard deviation, skewness, and kurtosis of the resultant. Following through these operations we can express these latter parameters in terms of those of the component distributions:

$$\bar{z}=a+b\bar{x}+c\bar{y}+\ldots+d\bar{t} \qquad (28)$$

$$\sigma_z=\sqrt{b^2\sigma_x^2+c^2\sigma_y^2+\ldots+d^2\sigma_t^2} \qquad (29)$$

$$k_z=\frac{b^3\sigma_x^3k_x+c^3\sigma_y^3k_y+\ldots+d^3\sigma_t^3k_t}{[b^2\sigma_x^2+c^2\sigma_y^2+\ldots+d^2\sigma_t^2]^{3/2}} \qquad (30)$$

$$\beta_{2z}=\frac{b^4\sigma_x^4(\beta_{2x}-3)+c^4\sigma_y^4(\beta_{2y}-3)+\ldots+d^4\sigma_t^4(\beta_{2t}-3)}{[b^2\sigma_x^2+c^2\sigma_y^2+\ldots+d^2\sigma_t^2]^2}+3 \qquad (31)$$

Equations 28 and 29 of course include the familiar relations:

1. The mean of the sum (or difference) of independent variates is the sum (or difference) of the means of the individual variates.
2. The standard deviation of the sum (or difference) of independent variates is the square root of the sum of the squares of the individual standard deviations.

It is perhaps not universally appreciated that these relations are true whatever the distribution forms of the component variates.

When n identical variates are to be added, which is a common case, equations 28 to 31 reduce to

$$\left.\begin{array}{l} \bar{z}=n\bar{x} \\ \sigma_z=\sqrt{n}\sigma_x \\ k=\dfrac{1}{\sqrt{n}}k_x \\ \beta_2=\dfrac{1}{n}\,[\beta_{2_x}-3]+3 \end{array}\right\} \qquad (32)$$

Thus the resultant's average is just n times an individual average, the standard deviation has increased at the rate of $\sqrt{n}$, while the skewness has diminished as $1/\sqrt{n}$, and the kurtosis is approaching 3 as a limit at a rate dependent on $1/n$. It will be seen that these last two limits, $k=0$ and $\beta_2=3$ are characteristic of the normal law. Likewise it can be shown that the same limits are approached for any linear equation of relation as n increases; moreover the primary variates need not all be alike. Hence we conclude that, if a large number of variates are combined linearly, their resultant will tend to approach the normal distribution with mean and standard deviation calculated from equations 28 and 29. As a practical matter, if no one distribution has a standard deviation substantially larger than the others to be combined linearly, the resultant of as few as six or eight components will nearly always be so closely normal that it may be considered normal for most engineering purposes.

EXAMPLE—RESISTANCE UNBALANCE IN WIRES

Two copper wires of 50 miles length are to be built from 120-pound coils (173 pounds to the mile, approximately), and the electrical resistances per coil vary with an equal likelihood over all values ±0.1 ohm from the average as shown in Figure 7. What is the probability that the completed wires will differ in resistance more than 1.5 ohms?

Solution. The number of coils n in each wire will be $n=\frac{50(173)}{120}\approx 72$. Hence the probability distribution for single wires will be closely normal about a mean value $\bar{r}$ equal to the average resistance per coil times the number of coils in the 50-mile length. The standard deviation of a single coil's resistance, by application of equation 2, is

$$\sigma_x^2=\mu_2=\frac{K\displaystyle\int_{-0.1}^{+0.1}x^2dx}{K\displaystyle\int_{-0.1}^{+0.1}dx}=\frac{\frac{2(0.001)}{3}}{0.2}=0.0033333$$

and $\sigma_x=0.05774$ ohm. Similarly we find $k_x=0$, and $\beta_{2x}=1.80$. Then by equation 32 the standard deviation for a single wire of 72 coils is $\sigma_{72}=\sqrt{72}\,\sigma_x=0.4899$ ohm. Likewise $k_{72}=\frac{1}{\sqrt{72}}0=0$, and $\beta_{272}=\frac{1}{27}\,[1.80-3.0]+3=2.9833$. The single-wire resistance distribution will then be closely normal, with the equation

$$\theta(r)=\frac{1}{0.4899\sqrt{2\pi}}e^{-\frac{(r-\bar{r})^2}{2(0.4899)^2}}$$

The distribution of the difference $z=r_1-r_2$ in two such wires will likewise be normal (from section 5.22.12) and will have the parameters, from equations 24 and 22

$$\bar{z}=\bar{r}_1-\bar{r}_2=0$$

$$\sigma_z=\sqrt{\sigma_{r1}^2+\sigma_{r2}^2}=\sqrt{2(0.4899)^2}$$
$$=0.6918\text{ ohm}$$

This distribution of z is shown in Figure 8. The probability that the two wires will differ in resistance more than 1.5 ohms, that is in excess of $2.168\sigma_z$, is read from a normal probability table or from Figure 5 to be 0.0302. The corresponding areas are indicated in Figure 8.

5.31.1 Distributions of Means

The reliability of the observed mean of a sample of n items is often desired.

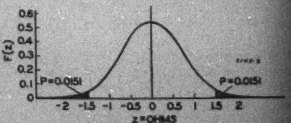


Figure 8. Difference between single-wire resistances

The true answer involves an application of *a posteriori* probability theory. However if n is large, say 100 or more, it is usually satisfactory to substitute the following *a priori* problem: If n items are drawn at random from a hypothetical universe with assumed parameters, what is the probability that a sample differing from this universe as far as that actually drawn could have resulted? This probability is then taken as a satisfactory approximation to the probability that the unknown true universe differs from the observed sample as far as did the hypothetical universe. The best estimate of the standard deviation of the unknown universe is the observed sample's standard deviation.

The distribution of the *means* of samples of n items is identical with that of the *totals* of n items when the latter's variate scale is reduced by $1/n$th. It is easy to see that the parameters of the means of successive samples of n items are

$$\bar{x} = \frac{1}{n}[\text{mean of the totals distribution}] = \bar{x} \tag{33}$$

$$\sigma_{\bar{x}} = \frac{1}{n}[\text{standard deviation of the totals distribution}] = \frac{\sqrt{n}\sigma}{n} = \frac{\sigma}{\sqrt{n}} \tag{34}$$

$$k_{\bar{x}} = k_{\text{totals}} \tag{35}$$

$$\beta_{1_{\bar{x}}} = \beta_{1\text{totals}} \tag{36}$$

Hence as n increases, the distribution of the means of successive samples of size n rapidly approaches the normal law, while the expected average is the universe average, and the standard deviation (or standard error) of the means decreases inversely as the square root of the number of observations n. This last, of course, agrees with the common sense conclusion that the larger the sample the more reliable its mean should be.

EXAMPLE—SAMPLING OF MANUFACTURED PRODUCT

A sample of 225 rough castings is taken at random from a shipment of about 10,000 and weighed individually. The average weight is found to be 11.72 pounds with a standard deviation of 0.804 pound. What is the probability that the average weight for the whole shipment is less than 11.60 pounds?

Solution. We solve the allied *a priori* sampling problem: If the true average is 11.60 pounds with standard deviation of 0.804 pound, what is the probability that a sample of the size drawn could have a mean of 11.72 *or higher?* The standard deviation of averages of samples with 225 items is estimated from equation 34 as $\sigma_{\text{avg}} = \frac{0.804}{\sqrt{225}} = 0.0536$. The discrepancy between the hypothetical universe mean and the observed sample mean is $11.72 - 11.60 = 0.12$, which in terms of σ_{avg} is $\frac{0.12}{0.0536} = 2.24$ standard deviations. From the cumulative curve of Figure 5 the probability of exceeding a deviation of 2.24 standard deviations is about 0.0125. We take this as the probability that the unknown true average in the shipment could lie below 11.60 pounds.

5.32 Approximate Product Combinations

In the formation of products the characterizing *moments* rather than the semi-invariants (which are so powerful in linear combinations) are found to be particularly useful. From equations 14 we can immediately write the moments of the product distribution (for discrete variates) as

$$\begin{aligned}\mu_i' &= \Sigma\Sigma \ldots \Sigma z^i p_r p_s \ldots p_t \\ &= \Sigma\Sigma \ldots \Sigma(rs \ldots t)^i p_r p_s \ldots p_t\end{aligned}$$

Since we are dealing with independent variates, this last expression may be rewritten as

$$\mu_i' = \Sigma r^i p_r \Sigma s^i p_s \ldots \Sigma t^i p_t$$

which in moment notation is

$$\mu_i' = \mu_{i_r}' \cdot \mu_{i_s}' \ldots \mu_{i_t}' \tag{37}$$

The same relationship can as readily be developed using continuous variates with integral signs replacing the summation signs. We conclude that *the ith moment about zero of the product is equal to the product of the individual ith moments about zero of the components.*

From equation 37 expressions showing the relationship between the parameters of the resultant and of the components are easily written. We find

$$\bar{z} = \mu_1' = \bar{r}\bar{s} \ldots \bar{t} \tag{38}$$

$$\sigma_z^2 = [(\sigma_r^2 + \bar{r}^2)(\sigma_s^2 + \bar{s}^2) \ldots (\sigma^2 + \bar{t}^2)] - (\bar{r}\bar{s} \ldots \bar{t})^2 \tag{39}$$

The values of the "higher" parameters can more readily be found in any specific case by numerical substitution in the basic equation 37, except perhaps in the case of just two variates.

Just as it was found that a linear combination of n independent variates tended toward the normal form as n increases, it may be shown by an analogous reasoning that the *product* of n independent variates tends toward the log-normal form. The log-normal distribution is a positively skewed unimodal curve whose lower limit is zero and upper limit is infinite and whose logarithms form a normal distribution. The chief restriction to this general conclusion on the trend of product distributions is that no one of the components should have the ratio of its standard deviation to its mean value very much larger than all of the other components' standard deviation-to-mean ratios.

5.33 Approximate Quotient Combinations

The problem of determining the parameters of a quotient distribution, given the corresponding parameters of the components, is complicated by the fact that values of the divisor variate must not ordinarily be permitted to occur at zero with any except a negligible likelihood. Approximate solutions must usually then be guarded by various restrictions regarding the distribution s in the quotient equation of relation $z = \frac{r}{s}$. If, however, we have a quotient distribution of the form $z = \frac{r + \bar{r}}{s + \bar{s}}$ in which $\bar{s}$ is several times the standard deviation of s, a satisfactory parameter solution can usually be found.

The most obvious, and oftentimes the easiest, procedure is to transform the equation of relation from a quotient type to the product type by merely changing the distribution of the variable in the denominator to its reciprocal's distribution. That is, if the equation of relation is given as $z = \frac{r}{s}$, then by setting $s = \frac{1}{w}$ we obtain $z = rw$ which is now a product of two independent variates. The subsequent analysis can then be handled as described in section 5.32 by taking products of the corresponding moments of r and w. Thus

$$\mu_i'{}_{z(=r/s)} = \mu_{i_r}' \cdot \mu_i'{}_{w(=1/s)}$$

This procedure will work in all cases to give the true moments of the final quotient distribution $F(z)$, except where the form of the w distribution is such that the moments cannot readily be determined. Even then a practical solution can sometimes be found, as suggested by Karl Pearson, by tabulating the reciprocals of the observations if the information on s is given in this form, and then calculating the moments of the reciprocal distribution. Likewise if the analytical expression for the probability distribution of the s variate is given, and this defies handling, the area under the probability curve may be divided into an appropriate number of class intervals, the area (observations) in each determined, and the procedure just outlined then carried through.

Fieller has given an exact expression for the distribution of $F(z)$ when the two variates r and s are normal. It is quite complex and for most engineering problems would be highly laborious to calculate. If the normal divisor variate s has a mean such that $\bar{s} \geqq 3\sigma_s$, then Geary has found that although z itself is not normal, a second variable dependent on z which we will call t is nearly normal. The variable t corresponding to any selected z is found from

$$t=\frac{\bar{s}z-\bar{r}}{\sqrt{\sigma_r^2+\sigma_s^2z^2}}$$

Then

$$F(z_1<z<z_2)\approx\frac{1}{\sqrt{2\pi}}\int_{t_1}^{t_2}e^{-\frac{t^2}{2}}dt$$

in which the value of the definite integral is readily read from a normal probability table or from the cumulative normal curve on Figure 5.

Craig and others have studied the general quotient problem and have established relationships between the final distribution's semi-invariants and those of the r and s components. If r and s are substantially normal and uncorrelated, and the s values occur with negligible likelihood at $s=0$, that is $\bar{s}>hs$ where h is say 5 or more, the quotient semi-invariants are given approximately by

$$\left.\begin{aligned}
L_1=\mu_{1z}'=\bar{z}\approx\frac{\bar{r}}{\bar{s}}\Bigg[1+\left(\frac{\sigma_s}{\bar{s}}\right)^2+ \\
3\left(\frac{\sigma_s}{\bar{s}}\right)^4+15\left(\frac{\sigma_s}{\bar{s}}\right)^6\Bigg] \\
L_2=\mu_{2z}=\sigma_z^2\approx\left(\frac{\bar{r}}{\bar{s}}\right)^2\Bigg[\left(\frac{\sigma_s}{\bar{s}}\right)^2+ \\
8\left(\frac{\sigma_s}{\bar{s}}\right)^4+69\left(\frac{\sigma_s}{\bar{s}}\right)^6\Bigg]+\Bigg[\left(\frac{\sigma_r}{\bar{s}}\right)^2+ \\
3\left(\frac{\sigma_s}{\bar{s}}\right)^2\left(\frac{\sigma_r}{\bar{s}}\right)^2+15\left(\frac{\sigma_s}{\bar{s}}\right)^4\left(\frac{\sigma_r}{\bar{s}}\right)^2\Bigg] \\
L_3=\mu_{3z}\approx\left(\frac{\bar{r}}{\bar{s}}\right)^3\Bigg[6\left(\frac{\sigma_s}{\bar{s}}\right)^4+ \\
116\left(\frac{\sigma_s}{\bar{s}}\right)^6\Bigg]+3\left(\frac{\bar{r}}{\bar{s}}\right)\Bigg[2\left(\frac{\sigma_s}{\bar{s}}\right)^2\left(\frac{\sigma_r}{\bar{r}}\right)^2+ \\
24\left(\frac{\sigma_s}{\bar{s}}\right)^4\left(\frac{\sigma_r}{\bar{s}}\right)^2\Bigg] \\
L_4=\mu_{4z}-3\mu_{2z}^2\approx72\left(\frac{\bar{r}}{\bar{s}}\right)^4\left(\frac{\sigma_s}{\bar{s}}\right)^6+ \\
84\left(\frac{\bar{r}}{\bar{s}}\right)^2\left(\frac{\sigma_s}{\bar{s}}\right)^4\left(\frac{\sigma_r}{\bar{s}}\right)^2+12\left(\frac{\sigma_s}{\bar{s}}\right)^2\left(\frac{\sigma_r}{\bar{s}}\right)^4
\end{aligned}\right\}\quad(40)$$

Naturally, the further either of the two component distributions deviates from the conditions stated above under which these equations have been derived, the more approximate they become.

Summary of Fundamentals

Probability or Frequency Curve Description of Variates

1. The majority of the essential information contained in a batch of data taken on a variate x can be summarized in a small number of statistical constants or parameters called the mean $\bar{x}$, standard deviation σ_x, skewness k_x, and kurtosis β_{2x}. These parameters are usually found by calculating the moments (or the semi-invariants) either of the observations or of the theoretical distribution describing the variate x.

Laws of Combining Simple Probabilities

2. When two events are mutually exclusive, the probability of one or the other occurring is the sum of their individual probabilities of occurrence.

3. When two events are independent the probability of their joint occurrence is the product of their individual probabilities of occurrence.

Combining Variates With Mutually Exclusive Probabilities

4. The parameters of the distribution z composed h proportion of the time by variate r, j proportion of the time by variate $s, \ldots,$ are given by

$$\bar{z}=h\bar{r}+j\bar{s}+\ldots$$

$$\sigma_z=[h(\sigma_r^2+\bar{r}^2)+j(\sigma_s^2+\bar{s}^2)+\ldots-\bar{z}^2]^{1/2}$$

$$k_z=\frac{1}{\sigma_z^3}[h(k_r\sigma_r^3+3\sigma_r^2\bar{r}+\bar{r}^3)+ j(k_s\sigma_s^3+3\sigma_s^2\bar{s}+\bar{s}^3)+\ldots-(3\sigma_z^2\bar{z}+\bar{z}^3)]$$

$$\beta_{2z}=\frac{1}{\sigma_z^4}[h(\beta_{2r}\sigma_r^4+4k_r\sigma_r^3\bar{r}+6\sigma_r^2\bar{r}^2+\bar{r}^4)+ j(\beta_{2s}\sigma_s^4+4k_s{}^3\bar{s}+6\sigma_s^2\bar{s}^2+\bar{s}^4)+ \ldots-(4k_z\sigma_z^3\bar{z}+6\sigma_z^2\bar{z}^2+\bar{z}^4)]$$

Combining Independent Variates According to the Equation of Relation, $z = \xi(r, s, t \ldots)$

5. The total probability of $F(z)$ is found from $F(z)=\mathfrak{S}p_rp_sp_t\ldots$ or $F(z)dz=\mathfrak{S}\theta(r)dr\times\phi(s)ds\psi(t)dt\ \ldots$ in which $\mathfrak{S}$ means the summation of the probability products for all possible ways of forming z according to the equation of relation.

6. $F(z)$ may sometimes be evaluated most conveniently for particular problems by a step-by-step method of combining probabilities.

7. If the probability distributions for the variates are known analytically, it is often possible to perform the $F(z)$ summations, obtaining an exact analytical expression for $F(z)$. Results typical of this method are:

(a). The sum of two Poisson variates is a third Poisson variate whose average equals the sum of its components' averages.

(b). The sum (or difference) of two normal variates has likewise a normal distribution whose mean equals the sum (or difference) of the means of the components, and whose standard deviation equals the square root of the sum of the squares of the components' standard deviations.

(c). The product of a square-topped and a simple type III Pearson variate yields the form

$$F(z)=\frac{c}{h-g}\left[e^{-\frac{cz}{h}}-e^{-\frac{cz}{g}}\right]$$

(d). The quotient of two generalized exponential (Pearson type III) variates has the form

$$F(z)=c^{n+1}q^{m+1}\frac{\Gamma(m+n+2)}{\Gamma(n+1)\Gamma(m+1)}\times\frac{z^n}{(cz+q)^{m+n+2}}$$

8. Approximate distributions of the resultant variate can be constructed if its parameters can be found. These can be expressed in terms of the components' parameters as follows:

(a). *Linear Equations of Relation.* $z=a+bx+cy+\ldots+dt$

$$\bar{z}=a+b\bar{x}+c\bar{y}+\ldots+d\bar{t}$$

$$\sigma_z=\sqrt{b^2\sigma_x^2+c^2\sigma_y^2+\ldots+d^2\sigma_t^2}$$

Expressions for k_z and β_{2z} are given by equations 30 and 31. From these we conclude that the sum of a large number of independent variates, no one of which has a predominant variation, tends rapidly toward the normal form with the parameters $\bar{z}$ and σ_z just given. Likewise, the standard deviation of the means of successive samples of size n is $1/\sqrt{n}$ times the standard deviation of the universe.

(b). *Product Combinations.* The mean and standard deviation of the product of variates $r, s, \ldots t$, is given by

$$\bar{z}=\bar{r}\bar{s}\ldots\bar{t}$$

$$\sigma_z^2=[(\sigma_r^2+\bar{r}^2)(\sigma_s^2+\bar{s}^2)\ldots(\sigma_t^2+\bar{t}^2)]-(\bar{r}\bar{s}\ldots\bar{t})^2$$

As the number of variates combined according to a product equation of relation increases, the resultant tends toward the log-normal form.

(c). *Quotient Distributions.* No rigorous method exists for obtaining the parameters of the quotient distribution in terms of the components' parameters. A number of estimates involving certain restrictions are given in section 5.33.

References

Simple Probability Distributions

1. Mathematical Statistics, H. L. Rietz. Open Court, 1927.

2. An Introduction to the Theory of Statistics, G. U. Yule, M. G. Kendall. Griffith and Company, England, 1937.

3. Elements of Statistics, part II, A. L. Bowley. Fourth edition, Scribners, 1920.

Laws of Combining Probabilities

4. Probability and Its Engineering Uses, T. C. Fry. Van Nostrand, 1928.

Exact Analytical Combination of Independent Variates

5. Frequency Arrays (booklet), H. E. Soper. Cambridge University Press, 1922.

6. The Combination of Observations, David Brunt. Second edition, Cambridge University Press, 1931.

7. Combining Two Probability Functions, W. D. Baten. *Annals of Math. Stat.*, volume 5, March 1934.

8. The Extended Probability Theory for the Continuous Variable With Particular Application to the Linear Distribution, H. P.

Series Capacitors for Transmission Circuits

E. C. STARR
FELLOW AIEE

R. D. EVANS
FELLOW AIEE

Synopsis: This paper presents the results of an analytical and experimental investigation of the use of series capacitors to increase the permissible loadings of long high-voltage a-c transmission lines. Because of limitations imposed by synchronous stability and reactive kilovolt-ampere requirements, conventional lines, when extended to the higher voltages and greater lengths, cannot be loaded to values of power sufficiently high to develop maximum over-all economy. It is shown that, when suitably applied, series capacitors will make possible the desired optimum loadings and will very materially reduce transmission costs.

The theoretical possibilities of using series capacitors to compensate the excessive inductive reactance of long lines have received consideration for many years. Practical series capacitors require protective equipment, but shunting of the capacitors in the earlier schemes resulted in a decrease instead of an increase in the transient-stability limits.

The authors propose series capacitors of the limited-voltage type in combination with auxiliary equipment which not only protects the insulation but quickly restores the capacitors to the circuit after the faulted conductors are isolated, thus preventing a decrease in system power limit at the time of need. The application requirements of necessary apparatus are also briefly outlined.

Results of analytical studies and miniature-system tests in connection with a typical application of series capacitors to one of the major lines of a typical transmission system are included. This work deals with relative transmission costs, transient stability, spontaneous hunting, and the subsynchronous operation in the induction starting of machines. The conclusion is reached that the proposed series-capacitor scheme appears practicable for long transmission lines.

THE necessity for long-distance transmission of large blocks of power has grown rapidly during the last few years. Some of the large-scale hydroelectric developments in the western states are located in relatively remote districts and have given rise to transmission problems involving distances of the order of 250 miles and blocks of power of the order of 300,000 kw. Proposed developments in other locations involve similar or longer transmission distances and equal or greater blocks of power. The three most important considerations in each of these major power developments are:

1. The conservation and most effective utilization of important natural resources.

2. The delivery of large blocks of power to natural load centers at minimum annual costs. With all factors given due consideration, these costs should not exceed those of other available power sources.

3. The delivery of large blocks of power to natural load centers with a high degree of reliability.

Due to basic circuit considerations, as transmission distances are increased, the voltages indicated become progressively higher, and the line costs increase at an accelerated rate. For the longer distances, power-limit or stability considerations, together with load and line reactive-kilovolt-ampere requirements, cause the unit power costs to rise rapidly. A basic analysis of the fundamental circuit properties involved indicates that, although a number of methods can be employed to improve the operating characteristics of long transmission circuits, the most promising expedient at the present time is line-reactance compensation by means of series capacitors. In the following sections, the theory and application of these devices as they relate to the costs and performance of major transmission circuits will be discussed in their essential details.

Modified Basic Analysis of Transmission Problem

Reduced to its fundamental elements, an a-c transmission line and its associated transformers constitute a connecting link of finite impedance between a generating station and its load, or between two electric systems. It has been shown that the power delivered over a circuit containing lumped impedance Z of angle θ, with voltages E_s and E_r maintained at the sending and receiving ends respectively, when these voltages are separated in phase position by the angle δ, is expressed by the following equation:

$$P_r=\frac{E_sE_r}{Z}[\cos(\delta-\theta)]-\frac{E_r}{E_s}\cos\theta] \qquad (1)$$

Resistance effects are ordinarily small in practical transmission circuits, and a close approach to the actual performance, when a reasonable stability margin is allowed, can be obtained by neglecting resistance in the preceding equation. The result is the simple expression as follows:

$$P_r=\frac{E_sE_r}{X}\sin\delta \qquad (2)$$

The power transfer is then a function of the product of the sending and receiving voltages, is a function of the angular displacement between these voltages, and is an inverse function of the circuit equivalent reactance interposed between the above voltages.

CONTROL OF CIRCUIT IMPEDANCE

The most convenient and economic voltage of generation is approximately

---

Paper 42-112, recommended by the AIEE committee on power transmission and distribution for presentation at the AIEE summer convention, Chicago, Ill., June 22-26, 1942. Manuscript submitted April 10, 1942; made available for printing May 18, 1942.

E. C. Starr is professor of electrical engineering, Oregon State College, Corvallis, Oreg., and consulting engineer, Bonneville Power Administration, Portland, Oreg. R. D. Evans is consulting transmission engineer, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

The authors acknowledge the assistance of A. C. Monteith and C. F. Wagner with the general problem, and of R. E. Marbury with capacitor design and protection, of J. D. Pierce for conducting the miniature-system tests, and of E. J. Harrington, H. M. Smith, and J. D. Curtin for analytical calculations.

Lawther. *Annals of Math. Stat.*, volume 4, November 1933.

9. The Distribution Laws of the Difference and Quotient of Variables Independently Distributed in Pearson Type-III Laws, S. Kullback. *Annals of Math. Stat.*, volume 7, March 1936.

Approximate Combination of Independent Variates

10 Theory of Observations, T. N. Thiele. Charles and Edward Layton, England, 1903; reprinted in *Annals of Math. Stat.*, volume 2, May 1931.

11. On the Constants of Index Distributions, K. Pearson. *Biometrika*, volume 7, 1910.

12. Frequency Distribution of an Index When Both Components Follow the Normal Law, A. S. Merrill. *Biometrika*, volume 20A, July 1928.

13. The Frequency Function of Y/X, C. C. Craig. *Annals of Mathematics*, volume 30, August 1929.

14. On the Frequency Function of XY, C. C. Craig. *Annals of Math. Stat.*, volume 7, March 1936.

15. On Frequency Distributions of the Quotient and of the Product of Two Statistical Variables, C. C. Craig. *American Mathematical Monthly*, January 1942

16. The Frequency Distribution of the Quotient of Two Normal Variates, R. C. Geary. *Journal* of the Royal Statistical Society, volume 43, 1930.

17. The Distribution of the Index in a Normal Bivariate Population, E. C. Fieller. *Biometrika*, volume 24, 1932.

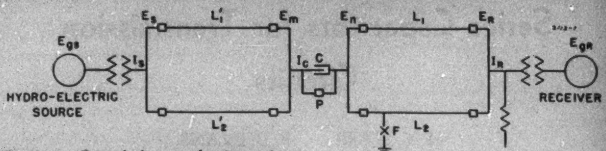


Figure 1. Typical diagram showing series-capacitor scheme of the limited-voltage type for use on long multiple-circuit transmission lines

C—Series capacitor
P—Protective equipment
L_1, L_2, L_1', L_2'—Sections of transmission line
E_{gs}, E_{gr}—Internal voltages at sending and receiving ends
E_s, E_m, E_n, E_r—Voltages on transmission line
I_s, L_r, I_c—Line currents

13.8 kv. Similarly, the best voltage for primary distribution in moderately heavy load areas and also for large individual loads is approximately 13.8 kv. If a transmission circuit must be employed between the generating station and load center, the above two voltages referred to a common base and maintained by regulating devices, are the quantities appearing as E_s and E_r in the preceding equations. The equivalent reactance X of the transmission circuit between the generating and receiving busses then determines in a large measure the amount of power than can be transmitted over this circuit.

If overhead transmission circuits are employed, the reactance of the conductors themselves will approach 0.8 ohm per mile at 60 cycles for any practical conductor and for all types of construction. It is evident that, as the transmission distance increases, the circuit reactance increases rapidly, and the power transmitting ability may thereby be reduced below the required value. Resistance effects, as controlled by conductor size, can be reduced from values giving transmission losses of the order of 15 per cent to practically zero without materially altering the power limit, although they do have a definite influence on the economic problem.

An approximate expression for maximum power, assuming 13.8 kv for sending and receiving voltages, and assuming 35 degrees between these voltages to allow for the reactance of terminal equipment and margin in stability, is as follows:

$$P_{rm} = \frac{110}{X}\, 10^3\,\text{kw} \tag{3}$$

The maximum power capacity is thus an inverse function of the equivalent reactance between the 13.8-kv generating and load busses of the transmission circuit.

If overhead line construction is to be retained, four different alternatives are available for reducing this equivalent reactance to the required value. These are:

1. Line-impedance conversion.
2. Multiple-circuit operation.
3. Reduction of system frequency.
4. Line-reactance compensation.

These items will be discussed in the indicated order in the following paragraphs.

HIGH-VOLTAGE OPERATION

For the reduction of equivalent series impedance, the first and most commonly employed expedient is impedance conversion which is accomplished by transformation to higher voltage. The well-known relationship between the high-side and the low-side equivalent impedance of a transformer, namely, that the equivalent low-side impedance of the high-side circuit is equal to the actual impedance of the high-side circuit divided by turn ratio squared, makes it possible to reduce the equivalent impedance between the low-voltage busses to a marked degree.

As an example of the effectiveness of a high-voltage transformation in reducing the equivalent impedance of a circuit, it can be observed that the reactance of an overhead line 100 miles long is approximately 80 ohms at 60 cycles. If line resistance were totally absent, if infinite busses were available at each end of the line, and if voltages of 13.8 kv were maintained at the terminals, the maximum synchronous capacity of the circuit would be approximately only 2,390 kw. A ten-to-one transformation on each end of the line, resulting in a line voltage of 138-kv, neglecting the reactance introduced by the transformers, would reduce the circuit reactance as it affects the low-voltage busses to 0.01 × 80 ohms or 0.80 ohm. This change, neglecting transformer impedances and line resistance and assuming infinite terminating busses, would result in a synchronous power limit 100 times that obtained originally, or 239,000 kw.

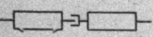
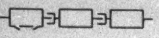
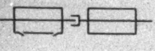
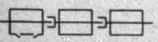
This particular expedient, namely, raising the line voltage by transformation to reduce the equivalent impedance between generator and load busses, has been followed to the practical exclusion of all other methods available for reducing this impedance. Voltages as high as 287.5 kv have been employed in gaining this end, and it will be observed that the equivalent impedance of a line of that voltage referred to a 13.8-kv bus is approximately 0.0023 times the actual line impedance.

It is evident that the only essential purpose of going to high transmission voltages is to reduce the equivalent impedance between the low-voltage busses at the terminals of the circuit. This one benefit, together with reduced equivalent surge impedance, which has been shown to be essential to the transfer of large blocks of power at high efficiencies, can be gained through high-voltage operation only at considerable expense. When voltages are increased, transmission structures become larger, conductor diameters, separations, and clearances become necessarily greater, and line insulation becomes more costly. Transformer and other high-voltage substation equipment costs increase much more rapidly than the voltages in the higher ranges, and transformer reactances, which add directly to the reactances of other elements of the circuit, increase more rapidly than the voltage rating.

Conductor diameters and desirable cross-sectionable areas are thrown so far out of balance at the higher voltages by corona loss, permissible line efficiencies, and mechanical considerations that, considering the necessarily great investment in the high-voltage system, electrical loadings approaching the values required to develop maximum transmission economies cannot be attained in the longer high-voltage lines. Most of the existing long, high-voltage circuits, because of stability limitations and reactive-kilovolt-amperes requirements, cannot be loaded to values exceeding approximately 50 to 75 per cent of the magnitude required to develop minimum annual costs per kilowatt transmitted. As an example of this limitation, it can be observed that the ratio of line loading for maximum economy to line loading for maximum power established by stability is approximately 2.5 in the case of a typical two-circuit 230-kv line somewhat less than 250 miles long and equipped with a mid-point sectionalizing station and normal low-reactance terminals. Even greater discrepancies exist in other more costly circuits of comparable length.

From the preceding discussion it is apparent that the expedient of ever

Table I. Relative Power Limits for Various Circuit Arrangements and Amounts of Capacitance Compensation

Case	Schematic Diagram	Compensation*	Relative Power Limits**
A		0	1.00
		20	1.15
		40	1.36
		50	1.50
		67	1.80
B		0	1.13
		20	1.33
		40	1.62
		50	1.80
		60	2.06
C		0	1.80
		20	2.14
		40	2.65
		50	3.00
		60	3.47
D		0	1.93
		20	2.32
		40	2.94
		50	3.38
		60	3.97

* Ratio of capacitive reactance to total reactance with all lines in service (per cent).

** Ratio of limits with single-line section out, using as reference case *A* without capacitive compensation. Resistance effect neglected.

higher transmission voltages, which are employed only for the purpose of reducing the equivalent system reactances, has reached and perhaps passed its justifiable limit until other steps are taken to strike a better balance among all factors concerned. The line-charging kilovolt-amperes of long high-voltage circuits is useful in most cases but detrimental in some. In those cases for which it is of benefit, it is obtained at relatively high cost.

It seems evident then that serious consideration should be given to other available means for decreasing the equivalent reactance between generator and load busses where long-distance transmission is necessary, and for increasing the electrical loading of these circuits to their points of maximum economy.

MULTIPLE CIRCUITS

The use of two or more transmission circuits in multiple results in a proportional decrease in the system transfer impedance, and when terminal ratings are increased in proportion, results in a similar increase in power capacity. Considerable improvement in operating characteristics and transient-stability limits can be obtained when such multiple circuits are sectionalized at one or more points proportionally distributed throughout their length. Individual line sections on which faults have occurred can then be isolated in only a few cycles after the initiation of the fault with a minimum of increase in total circuit reactance. The costs of multiple circuits with sectionalizing facilities go up in approximate proportion to the power capacities and consequently little is gained toward improving the economic loading status of long high-voltage circuits.

REDUCTION OF SYSTEM FREQUENCY

Since inductive reactance is a direct function of frequency, the loading of simple systems with given transformation ratios should, over the narrow practical range available, attain synchronous limits in approximate inverse proportion to the frequency employed. This expedient as a supplement to high-voltage transformation, or impedance conversion, has been employed in a number of foreign countries. However, because of the relatively small over-all benefit to be obtained, due to incandescent-lamp flicker at the lower frequencies, increased size and cost of induction equipment, and the necessity for numerous frequency changers, this method of gaining low reactance is not advisable and is being looked upon with disfavor in some localities where it is now practiced. The stability problem may not be avoided unless a considerable part of the load can be utilized at the transmission frequency.

D-C OPERATION

If the frequency is reduced to zero, that is, if direct current at high voltage is employed, power transfer is accomplished by difference of magnitude between terminal voltages instead of by difference between angular positions. It can be shown that the basic gain for direct current, when the same maximum insulation stresses are allowed, is produced by the 41 per cent higher effective voltage. This results in twice the power delivered over a given circuit having the same conductors and insulation and permitting the same transmission efficiency. Further advantages, however, are indicated. No synchronous stability limitations exist, and, if adequate terminal equipment can be made available, lines can be loaded to their economic limits. Moderately long a-c lines have synchronous load limits approximating their surge-impedance values. The economic limit employing direct current with the same conductors and insulation, in the average case, is approximately three times the a-c limit. If lines as long as 500 miles are required, this ratio would become larger than four. If it becomes necessary to transmit large blocks of power to points as remote as 500 miles, the d-c system will be particularly attractive. In that case the large saving in line costs over a-c requirements would make available very considerable sums for the necessary d-c terminal equipment. If this equipment can be made available at a figure that will increase the total terminal cost to a value approximately two times present a-c terminal cost, 500-mile transmission of large blocks of power can be made as economically feasible as some of the present systems of much shorter lengths. D-c transmission is not suited for supplying power to the intervening area, as there is no d-c equivalent for the a-c transformer. The major problem hinges upon the successful development of adequate terminal equipment.

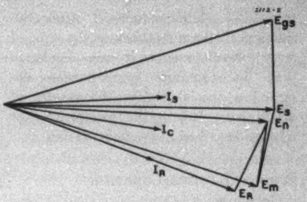


Figure 2. Vector diagram for series-capacitor transmission system shown in Figure 1

LINE-REACTANCE COMPENSATION

The most important alternative or supplement to high-voltage transformation for reducing the equivalent impedance between generator and load low-voltage busses is the use of line-reactance compensation. Such compensation can best be obtained through the use of static capacitors operating in series with the

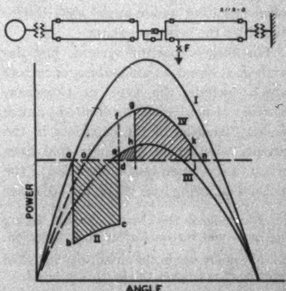


Figure 3. Power-angle diagram for sending end of series-capacitor transmission system connected to infinite receiver

ab—Fault on one line section, series capacitor shunted
ce—Faulted line section switched out
hg—Series capacitor restored to the circuit
k—Point of maximum angular swing
For stability,
area $(abcd) \leq$ [area $(edih)$ + area $(gijk)$]

Curve	Fault Condition	Capacitor	Line Sections Out
I	Before	In	None
II	During	Out	None
III	After	Out	One
IV	After	In	One

transmission-line conductors. After considering all these methods in the light of recent developments, it was concluded that line-reactance compensation deserved a special study, the results of which have led to the series-capacitor scheme described in the next section.

Proposed Series-Capacitor Scheme

The general arrangement proposed for using series capacitors for the compensation of the inductive series reactance of transmission circuits is shown schematically in Figure 1. This figure shows a protected or limited-voltage type of series capacitor located in a common-bus connection in a two-circuit line. The proposed arrangement would increase the permissible transmission-circuit loading, which is normally based on the system riding through a double line-to-ground fault and the subsequent circuit-isolating operation. Such an arrangement with series capacitors can be used to increase by 50 per cent or more the rating of the circuit, assuming that generating and receiving equipment are increased in proportion. Previous series-capacitor schemes actually reduce transient-stability limits,[1] because they lack the quick restoration feature of the present proposal. Vector diagrams for a particular load condition on the system of Figure 1 are given in Figure 2 which shows the large negative phase-angle shift introduced by the series capacitor.

The series-capacitor system, just described, is based on an analysis of factors which include: the type of capacitor, whether of nonlimited- or limited-voltage design; location of the capacitor in the circuit, whether in the individual lines or in a common-bus connection; the method of using the capacitor, whether

1. Normally in the circuit, shunted during the fault and restored after fault isolation.
2. Normally not in the circuit but switched into the circuit after the fault is isolated.

The factors also include the application problems of single-circuit and multiple-circuit lines.

Types of Capacitors

When systems with series capacitors are subjected to faults, high-voltage drops across the capacitors are produced by the increased line currents, and measures against overvoltage must be provided. Two types of capacitors may be used:

1. The nonlimited-voltage type in which sufficient insulation is provided in the capacitor to meet the fault condition.
2. The limited-voltage type which has insulation sufficient for all normal-circuit conditions and protective equipment to limit the excess voltages.

The nonlimited-voltage type of series capacitor has the desirable feature of always being available to provide reactive compensation, even during system faults. Unfortunately, the cost of the nonlimited-voltage type of capacitor is prohibitive, at least in comparison with the limited-voltage type. This results from the fact that, when high-speed fault clearing is employed, the capacitor cost is determined principally by the maximum-voltage condition and the capacitive reactance desired at 60 cycles.

The limited-voltage scheme uses

1. Capacitors that are much smaller and less expensive than those of the nonlimited type.
2. Protective equipment that not only limits overvoltages by providing a shunting path of negligible or low impedance but also opens this path promptly upon fault removal, incidentally reducing fault currents to the noncompensated value during the shunting operation.

Protective apparatus and the basis for selecting capacitor voltage ratings cannot be discussed here because of space limitations. Tests, however, have shown that suitable protective apparatus can be built. In the studies of system performance to determine the application requirements of series capacitors for transmission circuits, it is assumed that the protective equipment

1. Limits voltage on the first half-cycle of overvoltage.
2. Restores series capacitor to the circuit within two cycles after the fault is isolated.

Location in the Circuit

Series capacitors can be located at any point in a single-circuit line, but preferably near the reactance center of the system, as this limits the duty on the capacitor protective equipment, reduces the voltages to ground, and minimizes the problems of exciting-inrush currents. For multiple-circuit lines the series capacitor can be located in the individual line sections or in the common-bus connection, as shown in Figure 1. Multiple-circuit lines are normally laid out so that the circuit load can be carried with one line section out of service. For such conditions there is a decided advantage in using the series capacitor in a common-bus connection not only because the capacitor kilovolt-amperes is not reduced by the switching out of a line section, but also because fewer units are required. Several series capacitors may be used, but there is little advantage in using more than the number of intermediate switching stations.

Switching Arrangements for Series Capacitors

The condition of highest circuit reactance on a multiple-circuit line occurs as a result of switching operations that isolate a line section. Series capacitance may be required for the condition of highest circuit reactance but may not be needed for the normal condition with all lines in service. Hence, series capacitance can be used in two ways:

1. Series capacitor normally in the circuit, shunted for the fault condition, and quickly restored to the circuit after fault isolation.
2. Series capacitor not normally in the circuit, but switched into the circuit after a line section is switched out of service.

A combination of these arrangements so as to vary the amount of capacitance compensation according to the load or circuit arrangement is also possible. Both of these arrangements can be used with the layout of Figure 1. However, in the second arrangement the control equipment should be similar to that for the first arrangement in order to meet the condition for a second fault in the remaining section of the two parallel lines. The choice between these arrangements depends upon the relative advantages from the stability standpoint in starting from a condition of lower or higher system angle, as discussed in the section on stability.

Amount of Compensation

Stability and economic conditions control the amount of capacitive reactance to be used in any particular case. Assuming that generating and receiving systems are adjusted in proportion to the load transmitted, and that equal margins of stability are to be obtained, the relative stability limits can be estimated approximately from the inverse ratio of the 60-cycle transient reactances of lines including the series capacitor. Table I lists for several circuit arrangements and amounts of capacitance compensation the relative power limits calculated for the lines including capacitors but not terminal equipment. The amount of compensation is the ratio in per cent of the capacitive reactance to the total reactance with all line sections in service. The tabulated power limits are for one line section out of the circuit and are compared to that of a two-circuit line with one intermediate station but without series capacitors. Table I shows that, neglecting resistance, 67 per cent capacitive-reactance compensation is required with a two-line system and one intermediate station to give the same power limit as a system with three lines and one intermediate station and no series capacitors, assuming in both cases a transient disturbance requiring the isolation of a faulted line section. Similarly, considering the same two-circuit line carrying a given load, Table I shows for the same system and for the same stability margin that 50 per cent capacitance compensation is required for a 50 per cent increase in power limit.

Power-System Stability With Series Capacitors

Series capacitors increase the stability limits of systems by reducing the series or transfer reactance between internal voltages of machines, and thereby increasing the synchronizing power. Thus, series capacitors increase both the steady-state and transient stability of systems. Usually the transient-stability limits are well below the steady-state limits, so that a considerable increase in the former is possible before the latter condition becomes controlling. Nevertheless, it is desirable when considering series capacitors to investigate steady-state as well as transient limits.

The stability problem with series-capacitor systems can be viewed in the same manner as that of other systems by considering only the 60-cycle reactances of the circuit. It is, of course, necessary to take into account the changes in circuit reactance produced by the inclusion of the series capacitors in the circuit, and for the shunting of the capacitors during the fault and possibly for a brief interval thereafter.

A physical picture of the system phenomena during a fault and the resulting transient can be obtained from Figure 3. The system is assumed to consist of a hydroelectric generator feeding through a series-capacitor transmission system to an infinite bus, subjected to a fault at the point *F* which is cleared by the isolation of the faulted line section. The circuit is assumed to be without loss so that the power-angle diagrams are simple sine curves. Parts of four power-angle curves are plotted for the four conditions that arise as a result of the application of a fault and the subsequent circuit-isolating operation. These include:

1. The initial condition with all lines and series capacitors in service, curve I.
2. Condition 1 plus the application of the fault and the shunting of the series capacitors, curve II.
3. Normal condition but with one line section and series capacitor out of service, curve III.
4. The system normal with series capacitors in circuit but with one line section out of service, curve IV.

Consider the system of Figure 3 to be operating at the load and angle corresponding to the point *a* on curve I. Upon the application of the fault, the output of the generator drops to the point *b* on curve II. The difference in power *ab* accelerates the generator and increases the angle by which it leads the receiver. At the point *c* the fault is to be isolated by the action of high-spread breakers and relays, thus changing the operating point to *e* on curve III. Power corresponding to *de* decelerates the generator, but because of the accumulated velocity it swings forward along curve III. At the point *h* the series capacitor is restored to the circuit by the opening of the shunting path in the protective equipment, and operation proceeds from the point *g* on curve IV. Because the energy stored during the accelerating period has not been completely absorbed, the system continues to swing forward ultimately reaching point *k*, such that the decelerating area [area (*edih*) + area (*gijk*)] equals the accelerating area (*abcd*). The system is stable for the conditions shown and will oscillate about the point *o* and ultimately come to rest at that point. The system would be unstable for the conditions shown unless the series capacitors were restored to the circuit promptly after the isolation of the faulted line section.

If series capacitors of the nonlimited-voltage type are used, the curves of Figure 3 still apply; but upon the isolation of the fault at point *c*, the operating point changes instantly to the point *f* on curve IV. In this case the total area under the power-angle curve available for deceleration is increased by the area (*efgh*). There is, therefore, some advantage in reducing the interval required for the restoration of the series capacitor to the circuit after the fault has been removed.

Examination of Figure 3 shows the great importance of using high-speed breakers and relays for circuit isolation when series capacitors are used. The duration of the fault is of greater importance than the duration of the period in which the series capacitor is shunted by protective equipment. Fortunately, high-speed breakers and relays operating in four to six cycles are available. When these are used, the energy of acceleration is greatly reduced and is easily offset by relatively small increases in the power-angle curve above the transmitted load.

The power-angle curve II of Figure 3 is based on faults of the low-resistance type. If the system faults are of the high-resistance type, such as may occur on lines without ground wires, the resultant load on the generating station may be greater than the normal output. Under such conditions radically different transient conditions obtain, as illustrated in Figure 4a. For this case, upon the application of the fault the output of the generator immediately increases from *a′* to *b′* on curve II*a*. The power corresponding to *a′b′* decelerates the generator and reduces the angle by which it leads the receiver. At the point *c′* the faulted line section is cleared, and, assuming self-clearing protective equipment, the operation point changes at once to point *f′* on curve IV corresponding to the final circuit condition, with the series capacitor in the circuit, but with one line section out of service. Under this condition the power corresponding to *f′* produces a large accelerating action. However, the system, because of the accumulated velocity, continues to swing back and ultimately reaches the point

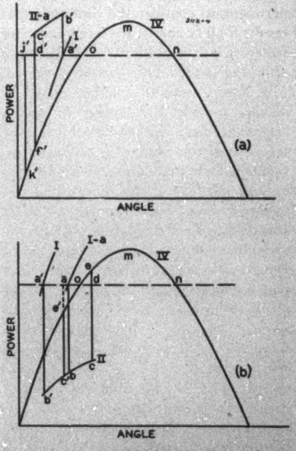


Figure 4. Power-angle diagram illustrating the effects of

(a). High-resistance fault

(b). Different initial angles

Curve I—Initial condition—all lines and capacitors in circuit

Ia—Initial condition—all lines but no capacitors in circuit

II—Fault condition—low resistance

IIa—Fault condition—high resistance

IV—Final condition—capacitors in circuit—one line section out of service

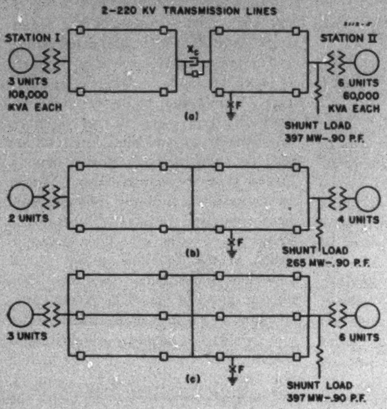


Figure 5. Schematic diagram of typical transmission line

Principal features of layouts (*a*), (*b*), and (*c*)

k' such that the accelerating area ($d'f'k'j'$) equals the decelerating area ($a'b'c'd'$). The power system now swings forward with increasing velocity corresponding to the accelerating area ($k'oj'$). For the power-angle relations shown in Figure 4a, the decelerating area (*omn*) is insufficient to absorb the resultant transient and instability results.

In connection with Figure 4a, it is pertinent to consider the condition that results if all or part of the series capacitor were not introduced into the circuit until after the faulted line section is cleared. This would result in a higher circuit reactance for the initial condition and a larger initial angle at the beginning of the transient disturbance. A little consideration will show that starting with a larger initial angle, say at the point *o*, the system will swing back through an angle slightly greater than before, but the maximum point reached on the backward swing will occur at an angle larger than that corresponding to the point k'. Consequently, the system will swing through a small angle, and for the conditions shown in the figure stable operation will obtain. Thus for some system conditions there is a gain from the stability standpoint in starting from a relatively large initial angle and switching the capacitors into the circuit only when they are needed because of the switching out of a line section.

For a different case shown in Figure 4b, the presence of a large series capacitor in the circuit initially is disadvantageous. The fault is assumed to be of very short duration, and the capacitance compensation to be quite large for the initial condition. Assuming again that the protective equipment is of the self-clearing type, the transition is made at once from the fault condition of curve II to the final circuit condition of curve IV. For the case without capacitors initially in the circuit and the transient starting from the point *a*, the accelerating area is *abcd*, and the area available for deceleration is *demn*. For the case with capacitance normally in the circuit and the transient starting from a smaller initial angle at a', the accelerating area is $a'b'c'e'o$, and the area available for deceleration is *omn*. A comparison of these two cases shows that the system is more stable when starting from the larger initial angle; that is, from the circuit condition with the higher circuit reactance or smaller capacitance compensation.

The conditions illustrated in Figures 4a and 4b have been introduced to show the various factors involved in the problem. However, if appreciable angular swing occurs during the fault so that the transition to the final circuit condition occurs in the vicinity of the point *o*, then there is usually an advantage in having the series capacitor in the circuit for the initial as well as the final circuit condition. This result will vary with the number of tie points in the system and the amount of change in circuit reactance. For the usual case, however, there is sufficient movement of the machines on the system to justify the inclusion of the series capacitor normally in the circuit.

RECLOSING BREAKERS

The advantage of reclosing breakers for improving system stability can be increased by the use of series capacitors in combination. This results from the fact that the power limit for the normal circuit conditions is large in comparison with the power limit with a line section out of service. This is illustrated by the large decelerating area under curve I in Figure 3, as compared with curve IV.

SINGLE-CIRCUIT LINES

Series capacitors can be used on single-circuit lines to increase the power limits, substantially in inverse proportion to the change in 60-cycle reactances. Such power systems are normally laid out to withstand disturbances that result from the loss of any transmission-line section, such as may result from the dropping of a line conductor. Accordingly, such systems are normally designed for operation close to the steady-state stability limit with the knowledge that faults on the circuit will normally result in circuit outage. Series capacitors can be used on such single-circuit lines to increase the stability limit, both for steady-state conditions and for transient disturbances resulting from faults on other line sections.

Single-pole switching with quick reclosing[3] is being used to improve the performance of single-circuit lines, particularly tie lines, from the standpoint of single line-to-ground faults. If series capacitors are added to such systems they will increase the power limit for the initial and final circuit conditions and on sound phases during fault condition. Such systems with series capacitors are, of course, subject to the limitations of other single-pole switching systems. Thus the length of line section must be limited or other measures used for insuring suppression of the arc to the faulted conductor when isolated at both ends. The system layout must be such that the time permissible from the stability standpoint is long enough to permit the reclosing cycle.

SPECIAL APPLICATION PROBLEMS

Actual transmission systems differ in a number of respects from the simplified conventional systems which have been considered hitherto in the discussion of the stability problem. For example, the preceding discussion has been based on

Table II. Characteristics of Motor-Generator Sets

Rating Damper Winding Reactance (Per Unit)	Motor Generator1 100 Kva—Copper	Motor Generator2 100 Kva—None
X_d'	0.260	0.260
X_q	0.577	0.577
X_2	0.151	0.418
T_{do}'	1.20	1.20
Inertia Constant H Kw-Sec per Kva	8.60	2.68

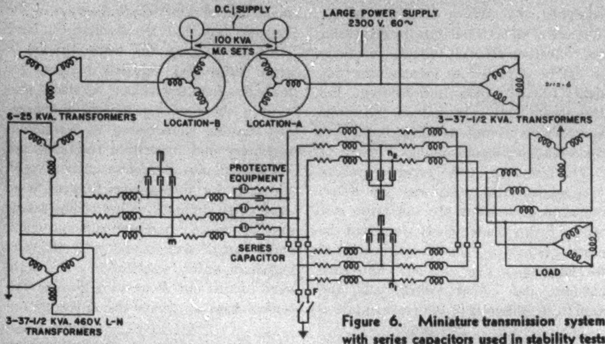


Figure 6. Miniature transmission system with series capacitors used in stability tests—simplified schematic diagram

resistanceless circuits supplied by hydrogenerators feeding into an infinite bus. The actual system includes loss and the receiver includes a shunt load with local generators also feeding into it. For the investigation of stability and similar problems, such as spontaneous hunting and subsynchronous operation in the induction starting of machines, it is advantageous to consider a typical transmission system suitable for series-capacitor application.

Typical Transmission System for Series-Capacitor Application

In order to develop further the characteristics of series-capacitor systems, a typical transmission circuit in a system undergoing considerable expansion was selected for theoretical analysis and subsequently for miniature-system tests. For these purposes simplified layouts approximating certain stages of the transmission development were selected. The principal features of these are shown schematically in Figure 5. In these layouts the output of two or three Station I generators is assumed to be transmitted over two or three 220-kv circuits for a distance of 234 miles. Three particular layouts selected for study are:

Figure 5a—Two 220-kv circuits.
Three 108 megavolt-ampere generators.
With series capacitors.

Figure 5b—Two 220-kv circuits.
Two 108 megavolt-ampere generators.
No series capacitors.

Figure 5c—Three 220-kv circuits.
Three 108 megavolt-ampere generators.
No series capacitors.

Miniature-System Tests

To complement the analytical work, miniature-system tests were made in the Westinghouse stability laboratory in order to examine further the operating characteristics of a system with series capacitors. The miniature-system tests included:

1. Stability.
2. Spontaneous hunting.
3. Subsynchronous effects in the induction starting of induction motors or synchronous machines.

The layout shown schematically in Figure 6 was used for the miniature-system stability tests. Two motor-generator sets, designated as M-G 1 and M-G 2, were used in set locations *A* and *B* respectively. These sets are identical, except for inertia and damper windings, and the characteristics affected by these features are given in Table II. Dampers are of the connected type in open slots.

The miniature-transmission system was adjusted to have circuit constants corresponding to the layout of Figure 5a, choosing for the generator the direct-axis transient reactance, X_d'. These constants and the miniature-system voltages were chosen so as to load the generator to the same proportion of its rating as the generators of the actual transmission system. The excitation for the generator was controlled by an exciter-rheostatic voltage regulator maintaining constant voltage on the transmission line. The receiver system was represented by transformers and impedance to the power supply corresponding to the transient reactance of the Station II generators and transformers, and by a shunt-impedance load.

Table III. Stability Limits of Various Systems for Double Line-to-Ground Fault

Layout	Series Capacitor	Station I Generator (Megavolt-Amperes)	220-Kv Lines	Miniature-System Stability Limit (Kw)
Figure 5a	61 per cent	324	2	104
Figure 5a	42 per cent	324	2	94
Figure 5b	None	216	2	61
Figure 5c	None	324	3	113

The series capacitors were connected between the two busses at the intermediate sectionalizing station. Tests were made principally with capacitive-reactance values of 61 per cent and 42 per cent of the total inductive reactance for two lines for both sections, although in a few hunting tests still other values were used. Each capacitor was provided with shunt protective gaps which included a low resistance in series to limit the discharge current through the protectors. A special self-clearing type of protective gap was used.

The layout to simulate the system of Figure 5b was obtained by correspondingly increasing the impedance of the generators, transformers, and loads in the ratio of 3 to 2. The layout corresponding to Figure 5c was obtained by reducing in the ratio of 3 to 2 the impedances of the series branches of the

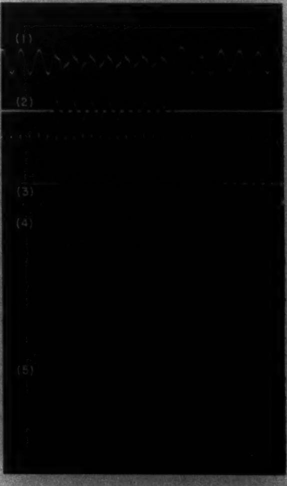


Figure 7. Typical oscillogram from miniature-system tests showing special protective gaps operating during double line-to-ground fault and self-clearing at the end of fault to restore capacitors to the circuit

1, 5—Capacitor voltages—phases *B* and *C*
2, 3—Gap currents—phases *B* and *C*
4—Voltage to neutral on phase *C*

two-lines section between Station I and the intermediate station and halving the series impedance of the unfaulted line-section between the intermediate station and the receiver.

The stability tests were run by adjusting the system to deliver the desired amount of power under specified voltage conditions. A fault was applied to one transmission line section by means of the fault breaker. Faults were of the zero-impedance, double line-to-ground type. The results of the stability tests are given in Table III.

Figure 7 shows an oscillogram for a stable condition corresponding to Figure 5a. The oscillogram shows a double line-to-ground fault of about seven cycles duration. Both protective gaps functioned during the fault condition and at the end of the fault were self-clearing. It will be noted that the voltages across the capacitors were limited by protective-gap breakdown during each half-cycle.

Observations with the aid of a stroboscopic device on the relative phase relation of the rotor of the set in the *B* location to the voltage of the power supply at receiver showed that the stability limits occurred for the three layouts of Figures 5a, 5b, and 5c at about the same phase displacements. The three systems, however, delivered different amounts of power for the same phase angle, the amounts of power being approximately in inverse proportion to the total 60-cycle transfer reactance between internal voltages.

Results of the tests given in Table III correspond to faults applied only at the intermediate station in the section between it and the receiver. This location required operation of protective gaps on the series capacitor, a circumstance unfavorable to the series-capacitor system in comparison with the layouts without series capacitors. The two sections of transmission circuit were interchanged, and the faults were applied at the sending end of the first section. The results of stability tests for this condition gave approximately the same ratio of stability limit for the layouts corresponding to Figures 5a and 5b as for faults at *F*.

Hunting Tests

Spontaneous hunting of synchronous machines is an infrequently encountered phenomenon on operating power systems.[3,4] Such hunting occurs only on systems having a high ratio of resistance to reactance, and on lightly-loaded machines and on machines which are not equipped with suitable damper windings. The use of series capacitors, by decreasing the effective 60-cycle circuit reactance would be expected to increase the tendency toward spontaneous hunting. For this reason tests were made on the miniature system using machines both with and without damper windings.

The first step in the experimental investigation of hunting was the determination of whether the miniature system of Figure 6 adequately simulated the electromechanical conditions in regard to hunting. The obvious difference between the actual system and the miniature system is in the representation of the prime mover. In the miniature system the prime mover is represented by a motor supplied by another motor-generator set obtaining power from an infinite bus. Tests under conditions for simulating hunting showed that motor-generator set *A* tended to follow the oscillation of motor-generator set *B*, introducing a damping action on it. Accordingly, a relatively high resistance was introduced in the supply to the d-c motor of set *B*. Under this condition, the ratio of power oscillations in output of generator *B* to motor input was over 50 to 1. This is shown in the oscillogram of Figure 8 by the single-phase watt element recording the output of the generator without dampers and the fluctuation in the direct current to the motor. It will be noted that the power oscillations in the two sets are substantially in quadrature. In order to increase the power output of set *B*, the voltage of the motor circuit was increased by adding a 250-volt shop supply in series.

Figure 8. Oscillogram from hunting tests showing the oscillation-damping action of motor-generator set at location *A* of the miniature-transmission system of Figure 6

A physical explanation for spontaneous hunting has been given by C. F. Wagner.[3] He showed that the tendency toward hunting depends upon the operating point on the power-circle diagram of the system, and that the oscillations have positive damping for loads beyond a given value and negative damping for loads less than this value. As stated previously, the tendency toward spontaneous hunting is increased as additional resistance and capacitive reactance are introduced into a circuit. Accordingly, the tests for spontaneous hunting were made with different values of line resistance introduced symmetrically into each equivalent *T* section and with different values of series capacitance. The tests were carried out so as to determine the critical load at which the tendency toward spontaneous hunting would be encountered. This was done by setting up the system for the desired operating condition and introducing an oscillation produced by a switching operation. The conditions were carefully examined to determine whether the oscillations corresponded to positive or negative damping. By observing the switching oscillations at several different loads it was possible to determine the critical point. Tests were carried out with both the sets, thus giving tests on

1. A generator without dampers.
2. A generator with copper dampers.

The results of the tests on spontaneous hunting are summarized in the curves of Figures 9 and 10. In Figure 9 the conditions in regard to line resistance and amount of series capacitive reactance are plotted in per cent of the normal line reactance for two circuits and two sections, not considering the reduction due to the series capacitor. In these curves the critical load is expressed as a per cent of the transient stability limit for the corresponding case as listed in Table III using the values corresponding to normal line resistance for all cases. Figure 10 gives the same data as Figure 9 but is plotted in terms of the total resistance and total transient reactance at 60 cycles between internal voltages, taking into

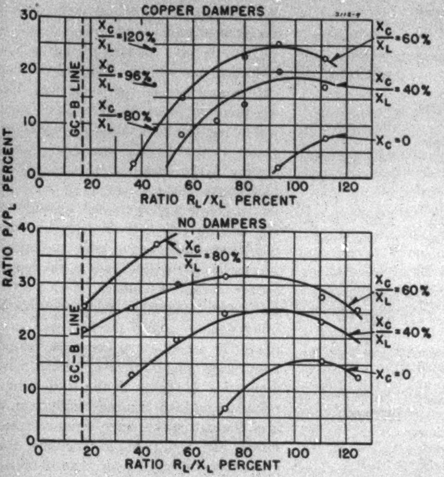


Figure 9. Critical conditions for spontaneous hunting from miniature - system tests—no hunting for points above curves

R_L, X_L—Resistance, reactance of two-circuit lines

X_C—Series-capacitor reactance

P/P_L—Ratio of transmitted power to stability limit

account the reactance of the series capacitor. The miniature system using the generator with dampers when laid out to represent the layout of the typical transmission system was stable for normal values of line resistance, even when series capacitors were used of higher reactance than contemplated for the amount of power to be transmitted. The miniature system using machine without dampers was stable when transmitting a reasonable amount of power in comparison to the stability limit. Hunting under light load is not considered important as the capacitors can be switched out of the circuit because they are not needed under this condition for stability.

A special hunting test was made to show whether series capacitors affected the hunting conditions except by changing the 60-cycle reactance of circuit. For this purpose two circuits of equal resistance and equal 60-cycle reactance were compared using

1. A circuit with only inductive branches.
2. A circuit with additional inductive reactance offset by equal capacitive reactance.

Using the generator without damper windings, no hunting was found with the inductive circuit but there was hunting with the series capacitors, although the critical load was only $2^1/_2$ per cent of the transient stability limit.

Since the spontaneous hunting has received but relatively little attention in recent years, there are little data to show how the damping on a miniature system compared with that of actual power systems. Accordingly, tests were made on the miniature-system setup for the layout of Figure 5a with a 60 per cent series capacitor, but with the system carrying half load. A line section was switched out of service and then restored, the latter transient being recorded on oscillograms, both for the machine without damper windings and for the machine with copper dampers. For the machine without dampers the natural frequency was 43 cycles, while for the machine with copper dampers the natural frequency was 80 cycles. The oscillations for these cases were reduced by about 7 and 30 per cent respectively per cycle of electromechanical oscillation. It is proposed that tests be made on the actual power system, and the results compared with those obtained from similar tests on the miniature system.

Induction Starting of Machines

An interesting test was made to simulate the induction starting of induction motors or synchronous machines. This test was made with the layout of Figure 5a with a 60 per cent capacitor but with the load and receiver system disconnected at the end of the second transmission section of Figure 6. A 10 horsepower squirrel-cage induction motor was connected at the receiving end of the transmission system. Under normal voltage conditions the motor started without any difficulty. The generator voltage was reduced to about 20 per cent of normal in order to simulate the starting of an induction motor with a heavy inertia load, and under this condition, the motor operated at a subsynchronous speed. This phenomenon of subsynchronous operation[5] is caused by resonance at a frequency below 60 cycles. At normal voltages on the system the induction motor has sufficient torque to overrun the subsynchronous speed before the torque caused by low-frequency resonance can build up. At low system voltages, the motor acceleration is slower and the torque caused by low-frequency resonance builds up to a higher value. In spite of this the motor accelerated beyond the subsynchronous speed but was unable to reach normal speed and dropped back to the subsynchronous speed. As was expected, a load of high power factor, about 95 per cent, drawing as much current as the accelerating current of the motor was sufficient to avoid operation at a subsynchronous speed and to permit acceleration to normal speed. This peculiar condition is considered of little importance from a practical standpoint, since no normal operating condition is visualized in which the difficulty would be encountered. The nearest approach is the condition of starting up a system with the series capacitor in the circuit followed by the induction starting of a large synchronous condenser at the receiver. Under these conditions diffi-

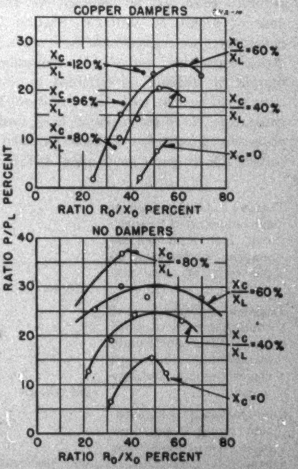


Figure 10. Critical conditions for spontaneous hunting from miniature-system tests—no hunting for points above curves

R_L, X_L—Resistance, reactance of two-circuit lines

R_0, X_0—Total circuit resistance and reactance

X_C—Series-capacitor reactance

P/P_L—Ratio transmitted power to the stability limit culty may be encountered in accelerating the condenser to normal speed. The difficulty can, of course, be overcome by switching the capacitor out of service until the condenser is in synchronism. If considerable generating capacity or considerable shunt load is connected in parallel with the condenser, no difficulty in starting is to be expected.

Economic Advantages of Series Capacitors

An example of the substantial economies that can be effected by the judicious use of series compensation in appropriate circuits will now be given. In this example the two-circuit, 230-kv line between the Station I and the Station II developments is assumed to be compensated to bring it more nearly in line with the load capacity required to develop maximum transmission economy. An additional generator at Station I and a corresponding increase in generator and load capacity at the Station II end are assumed. Series capacitors giving $66^{2}/_{3}$ per cent compensation are to be located at the mid-point of the transmission line, as in Figure 5a. This series capacitor will give with three Station I generators and two lines a greater margin of stability than will be obtained for the case of Figure 5b with two Station I generators, two transmission lines, and no series capacitors, and closely the same margin of stability as obtained in the case of Figure 5c with three Station I generators, three transmission lines, and no series capacitors. The calculated results of the economic study are as follows:

Transient stability limit of uncompensated two-circuit line with normal margin (load reduced 20 per cent for same machines)	175,000 kw
Transient stability limit of compensated two-circuit line with proportionally increased terminals with corresponding stability margin	290,000 kw
Line-reactance compensation required on complete two-circuit line base	66.7 per cent
Series capacitor required	108,000 kva total
Series capacitor installed cost at $11.00 per kilovolt-ampere	$1,190,000
Cost of one additional circuit with sectionalizing facilities (based on minimum construction cost)	$4,500,000
Saving in capital outlay through use of series compensation, or 73.5 per cent of cost of third circuit	$3,310,000
Transmission efficiency:	
Three-circuit line, full-load	95.0 per cent
Two-circuit line, compensated, full-load	92.4 per cent
Increased losses, two-circuit line as compared to three-circuit line, all sections operating	8,560 kw at max. load
Incremental loss chargeable to compensated two-circuit line at 85 per cent loss factor (equals approximately 82 per cent load factor)	6,180 kw
Cost of generating incremental loss chargeable to compensated two-circuit line at $125.00 per kilowatt generation cost	$772,000
Incremental short-time synchronous-condenser capacity required by the two-circuit compensated line at full load with one section out over that required by three-circuit line at full load and one section out	42,800 kvar
Incremental cost of synchronous-condenser capacity at $4.00 per reactive kilovolt-ampere (over-load rating)	$171,500
Total cost of compensation, or 47.4 per cent of the cost of the equivalent third circuit	$2,133,500

The above conservative example illustrates clearly the very considerable economy that can be effected by the proper use of series compensation. If high emergency-load capacity is the primary consideration, the aggregate increase in losses is negligible, and the comparison is even more favorable.

In the case of heavy impedance loads, or of lines materially shorter than the one chosen in the above example, the loadings of maximum economy may be attainable without compensation and without giving rise to any synchronous stability problem. In such cases, excessive voltage regulation instead of instability will be the major controlling factor. The circuit loadings will be considerably in excess of the normal surge-impedance values, and series-reactance compensation can be employed to improve the voltage regulation and the power factor at the generator end of the line.

A further study of the particular application, however, may in certain cases disclose that the same kilovolt-ampere rating in shunt capacitors applied to the low-voltage bus at the receiving-end of the transmission circuit would result in slightly better voltage regulation and efficiency conditions than when that capacitor is used as a series device. This difference in favor of shunt application will be particularly evident when the load power factor is near unity or slightly lagging. The application of shunt capacitors in this case results in improved power factor and decreased transformer and line currents with decreased voltage regulation and reactive kilovolt-amperes generated in the line and in the transformer leakage reactance. If such a circuit were operating independently of other systems, and if the load were of a relatively steady character, the shunt application might prove to be the more economic. However, if the load is supplied by a multiple transmission system, or is of fluctuating character, series compensation can result in considerably improved performance over that obtained when the necessary capacitive kilovolt-amperes are used in shunt on the load bus. In such cases the problems require individual analysis to determine the most economic expedient to obtain the desired end, both as regards economy of operation and system performance.

When synchronous stability limitations prevent the most economic loadings, series compensation, up to a certain degree, will in general be found to give better economies than other alternatives. If increased equivalent surge-impedance load ratings are required, the line capacitive susceptance can in effect be increased by shunt capacitors on the low-voltage receiving bus by the same factor that the line equivalent reactance is decreased by series compensation.

Requirements of Series Capacitors and Protective Equipment

Results of the analytical studies and miniature-system tests lead to the following conclusions as to the requirements of series capacitors and their protective equipment.

Series capacitors should be of the limited-voltage type and provided with protective equipment which

1. Operates on the first cycle of overvoltage to limit the voltage stress on the insulation.
2. Restores the capacitor to the circuit in the shortest practical time after the faulted line section is cleared, preferably within not more than two cycles.

Provision will be desired in some cases to switch the capacitor into the circuit only after a line section is switched out of service. Additional protective features should be provided to prevent sustained high currents which would produce important heating effects. The protective equipment should include means for switching the capacitor into the circuit and out of the circuit as may be desired locally for inspection or repair, or by remote control as may be found advantageous from the standpoint of system operations. A small impedance in the shunting circuit is advantageous in limiting condenser discharge current; in addition, resistance tends to damp transient currents and improve stability.

Conclusions

Analytical studies, including the economic problem, have shown that long high-voltage transmission circuits can be operated at their optimum loadings only when their equivalent impedances, or effective lengths, are reduced by a sizable factor. This reduction in transfer impedance can be accomplished most economically through the use of series compensation. Such applications on appropriate systems would result in considerable over-all economic gains. Although in the shorter lines of lower voltage, independent economic gains may not be possible through the application of series compensation, these circuits can be increased in emergency capacity and electrical stiffness to a marked degree by the application of appropriate reactance compensation.

The analytical work and the miniature-system tests have shown that the stability of transmission systems with series capacitors can be estimated from consideration of the 60-cycle constants of the circuit in the conventional manner. Such estimates or calculations of stability should take into account the decreased circuit impedances that result when series capacitors are in the circuit and also the transient changes that result from the operation of protective equipment.

Transmission systems with series capacitors are not subject to spontaneous hunting, providing machines are equipped with suitable damper windings, or provided the system is not operated at too low a percentage of its normal rating. By observing practical operating restrictions at light load, it is possible to avoid problems of hunting and subsynchronous operation in the induction starting of machines.

The proposed scheme of using series capacitors of the limited-voltage type, in combination with protective equipment that quickly restores the capacitor to the circuit after fault isolation, is viewed as providing attractive possibilities for increasing the permissible and economic loading of long transmission circuits. Series-capacitor schemes have inherent advantages for increasing the permissible short-time loads or for increasing the emergency loads of existing systems. The scheme is viewed as sufficiently promising as to justify a large-scale experimental installation.

References

1. **First Report of Power-System Stability** AIEE subcommittee on interconnection and stability factors. AIEE Transactions, volume 56, 1937, February section, pages 261–82.

2. **High-Speed Single-Pole Reclosing, J. J. Tranior, J. E. Hobson, H. N. Muller, Jr.** AIEE Transactions, volume 61, 1942, February section, pages 81–7.

3. **Effect of Armature Resistance on the Hunting of Synchronous Machines, C. F. Wagner.** AIEE Transactions, volume 49, 1930, pages 1011–24.

4. **Analysis of Series-Capacitor Application Problems, J. W. Butler, C. Concordia.** AIEE Transactions, volume 56, 1937, August section, pages 975–88.

5. **Self-Excitation of Induction Motors, C. F. Wagner.** AIEE Transactions, volume 60, 1941, pages 1241–7.

6. **The Series-Capacitor Installation at Ballston, New York, E. K. Shelton.** *General Electric Review*, August 1928, pages 432–4.

7. **The Effect of Series Capacitors Upon Steady-State Stability of Power Systems, E. J. Allen, J. L. Cantwell.** *General Electric Review*, May 1930, pages 279–82.

8. **Stability Limitations of Long A-C Transmission Lines, Edith Clarke, S. B. Crary.** AIEE Transactions, volume 60, 1941, pages 1051–9.

Lightning Investigation on 132-Kv Transmission System of the American Gas and Electric Company

Discussion and authors' closure of paper 42-18 by I. W. Gross and G. D. Lippert, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, April section, pages 178–85; previous discussions published in AIEE TRANSACTIONS, 1942, pages 450–3.

R. H. Golde (British Electric and Allied Industries Research Association, London, England): The maximum values of lightning currents which may flow in the various parts of a transmission system are of great interest to all engineers concerned with the protection of overhead lines, and the authors' continued collection and latest presentation of statistical data are a most valuable addition to our knowledge of this subject. In their discussion, however, the authors draw attention to two features in their results which they suggest are in conflict with the present knowledge of surge phenomena and, in particular, that relating to the effective surge impedance of line conductors. The probable explanation of these results is as follows:

Table A gives the maximum voltage values which would be obtained if the maximum line currents plotted in Figure 5 of the authors' paper were multiplied by a surge impedance of 400 ohms. The resulting products, if regarded as voltage surges on the line conductors, are rightly described as fantastic. I should therefore like to suggest that the relatively few high current values given in Figure 5 were actually measured in cases in which a direct stroke occurred to a line conductor somewhere in mid-span with immediate flashover to earth at the first tower or towers encountered. In this case the short length of line wire between the point struck and the first tower carries practically the whole lightning current, but as its length is small compared with the equivalent length of the front of the average lightning current, it is misleading to attribute to it any surge-impedance value. The discharge phenomenon is simply that of a charged condenser—the lightning channel—discharging to earth through a short path with its inherent capacitance and inductance values, the conditions being very different from those obtaining for a long line conductor. The resulting surge voltages which are propagated along the line would then be given by the line currents measured beyond the region where discharge to earth takes place multiplied by the correct surge impedance of the line.

The second point which, according to the authors, "requires some further study or interpretation" concerns the line surge impedance values derived in Table II. These values are much too small to be explained by any known effect, including that of corona. Now it appears that the current and voltage values quoted in Table II were measured when discharge of the arresters protecting the stations in question took place. Under these conditions reflection phenomena invalidate the calculation of the line surge impedance as the quotient of the bus voltage and line current. An accurate calculation could be carried out only if the discharge characteristic of the arrester, the shape of the incident surge, and the distance between the recording points and the discharging arrester were known. If, in the absence of these data, it be assumed, as a first approximation, that the measured line currents and bus voltages reach their crest values at the same instant and that the incident surge has a very short front and long tail, then

$$E=e\,\frac{2R}{Z+R}$$

and

$$I=i\,\frac{2Z}{Z+R}$$

where

E = busbar voltage
e = voltage of incident surge
I = resulting line current during operation of arrester
i = current of incident surge
Z = line surge impedance
R = equivalent discharge resistance of arrester

Since e/i is equal to Z, it follows that the quotient E/I is independent of the line surge impedance and is in fact equal to the equivalent discharge resistance of the arrester at the instant when both bus voltage and line current reach their maximum. The resistance values quoted in Table II are quite reasonable if thus explained, and there is no need to invoke any change in the accepted value for the line surge impedance.

There are several items on which further comments might be made but only the most important may be mentioned. Returning to Figure 5, it has been indicated that the highest current values recorded are probably not line currents in the sense of surges which are propagated along the line. Assuming that currents of the order of 3,000 amperes are the highest which can travel along the line conductors without causing flashover, it is seen from the two curves in Figure 5 that some 75 per cent and 90 per cent respectively of the currents recorded fall within that range. These currents may therefore be due to cases of back flashover, though it would also be interesting to get the authors' information as to the maximum possible earth fault currents on these systems, since it may be feasible to explain at least some results by normal frequency earth fault currents following a flashover at one or more towers. It follows that Figure 5 consists of two parts, that is, effects of direct strokes to line wires and cases of back flashover, which are not strictly comparable.

The beneficial effect of bonding the tower legs below the ground surface is convincingly shown by the currents discharged through these connections. The authors regard this effect to be due to "an even, uniform, and quick distribution of current to the various tower structural members." It is difficult to see why the lowest cross members of lattice towers should not be equally effective, and I should suggest that the effect described is primarily due to these additional connections acting as earth electrodes.

The good result to be obtained by the use of long earthing tubes, particularly in soil with a top layer of high resistivity, has long been recognized by practical experience. The authors' current measurements provide, therefore, interesting direct confirmation. On the other hand, the meager effect of a ten-foot tube (Table III, case 2), installed as shown in Figure 2, is hardly surprising, since this tube which is placed between tower legs of equal length to itself cannot be expected to contribute materially to the combined earthing resistance of the tower. It would be interesting if the authors could install some earthing tubes at the ends of the 40-foot counterpoise wires and determine the current distribution for that case.

I. W. Gross and G. D. Lippert: The discussion of the paper by Mr. Golde is most welcome, and we are gratified to note that the data are helpful in furthering the study of lightning phenomenon. Some of the items discussed by Mr. Golde have been previously discussed by Mr. Hagenguth and others and have been commented upon by the authors. Further comment on these items by the authors at this time does not appear necessary.

The surge crest ammeter links on the line conductors were so adjusted and calibrated that the maximum possible 60-cycle fault currents (in the order of 2,500 amperes) would not materially affect the results.

The beneficial effect of bonding the four tower legs together below ground is apparently not obtained by the diagonal bracing of the tower above ground. Lightning-current measurements (not reported in the paper) made on four leg towers above the point of connection of the first lattice braces indicated the lightning current was concentrated in the two legs directly connected to the counterpoise where the underground bonding was not in use.

The 10-foot ground rods reported in the paper were not installed with any expectancy of obtaining an appreciable decrease in the over-all tower-footing resistance. These short rods were installed for the sole purpose of studying their performance in combination with longer rods. It was believed that the effect of these short rods would be largely obscured by the longer rods and the 250-foot counterpoise connections, and the observed results to date have indicated that this is precisely what happens.

Rectifier Terminology and Circuit Analysis

Discussion and authors' closure of paper 42-83 by C. H. Willis and C. C. Herskind, presented at the AIEE North Eastern District meeting, Schenectady, N. Y., April 29–May 1, 1942, and published in AIEE TRANSACTIONS, 1942, July section, pages 496–9.

I. K. Dortort (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): A uniform system of terminology for the circuit elements of rectifier systems is highly desirable, and any attempt to bring some order out of the present confusion deserves everyone's co-operation. To that end, it is suggested that we retain certain terms of the present terminology.

The greatest disagreement probably exists concerning the terms half-wave, full-wave and single-phase rectifiers. The true single-phase rectifier is recognized as a half-wave rectifier by all. It is nothing more than a simple check valve seldom used in power circuits. If we dispose of it by giving it some such name as "simple half-wave" or "valve," the problem of naming the other types of circuits is very much simplified. We could then go back to the terms half-wave and full-wave rectifiers, terms which are generally acceptable and understood by the majority of people using rectifiers. The terms would apply strictly to the conductively connected transformer windings or circuit elements.

The terms suggested by the authors are similar to the German "one-way" and "full-way" connections. Hermann in *Elektrotechnische Zeitschrift*, December 1938, does not indicate that the use of these terms has improved the situation in Europe over our own.

It is believed that it would be better to remove the full-wave or "double-way" rectifier from under the heading of "simple rectifier." The full-wave rectifier partakes of the properties of both the "multiple" and "cascade" rectifiers in that the d-c voltages are added, but the ripple voltages do not coincide, producing a ripple of higher order. This applies quite generally to any cascaded half-wave or full-wave rectifiers whose phases do not coincide.

More thought should be given to the problem of properly naming rectifying devices or valves which may be used for purposes other than rectification. They may perform the function of inversion, frequency changing, commutation for "commutatorless motors." The function of inversion and rectification may be embodied in one device. In all of the functions listed here the rectifiers act as commutators. Some years ago Brown Boveri adopted the generic term of "mutator."

An easily evaluated factor providing a common denominator for a large number of rectifier circuits will certainly prove useful. To a very limited extent, the percentage reactance of the transformer has been used that way in the past. The reactance factor given in the paper can be readily modified to make it universally applicable at least to the commonly known and used power rectifier circuits.

The commutating reactance is the total lumped reactance between two anodes which undergo commutation, divided by two. In other words, it is the reactance attributed to each anode in a balanced system. This quantity is called x_c in the paper. In the same way we can call the commutating voltage, E_c, one-half the voltage appearing between the two anodes. This is, of course, the anode to neutral voltage, $E_s \times \sin(\pi/p)$. The peak value of the current which would flow in a short circuit between these two anodes is, $(\sqrt{2}E_c)/X_c$. Let us call this value I_c'. The shape of the anode current during commutation is a segment of a sine wave of this amplitude.

The ratio $(I_cX_c)/(\sqrt{2}E_c)$, where I_c is the total direct current to be commutated by an anode, is akin to the so-called percentage reactance of the transformer and might be called the per unit reactance drop. Actually it is equal to I_c/I_c' and could also be called the per unit load based on the peak short-circuit current.

If one uses $(I_cX_c)/(\sqrt{2}E_c)$ as the reactance factor and calls it F_x for convenience, the equation for the overlap becomes $\cos(u + oc) = \cos\alpha - F_x$. This relationship holds for any number of phases.

Another interesting property of this modified reactance factor can be demonstrated very easily. The theoretical no-load voltage of the rectifier is $\dfrac{\left(\sqrt{2}E_s \sin\dfrac{\pi}{p}\right)}{\dfrac{\pi}{p}}$. This can be converted to or derived directly[1] from Goodhue's relationship to $\dfrac{\sqrt{2}E_c}{\dfrac{\pi}{p}}$. The drop in voltage on the d-c side due to reactance is $\dfrac{I_cX_d}{\dfrac{2\pi}{p}}$ as shown in the paper. The per unit d-c voltage drop is therefore $^1/_2[(I_cX_c)/(\sqrt{2}E_c)]$ or $^1/_2F_x$, regardless of the number of phases or the types of windings! And this ratio holds true whether it is a half-wave or full-wave rectifier! Should the need ever arise, this relationship can be applied to unsymmetrical circuits by readily means of Goodhue's rectifier calculus.

The curves given for the a-c current harmonics and d-c voltage ripple harmonics apply only to a restricted number of systems. Since F_x is a common parameter for more systems than $(I_cX_c)/E_s$, curves plotted against F_x should have more general application.

REFERENCE

1. SELF-REGULATED COMPOUND RECTIFIERS, W. Melvin Goodhue and R. Burton Power. AIEE TRANSACTIONS, volume 55, 1936, pages 1200–5.

C. H. Willis and **C. C. Herskind:** The modification of the reactance factor as suggested by Mr. Dortort so as to make it universally applicable to the different types of rectifier circuits has been considered by the authors. Such modification would be desirable if it provides the simplest and most convenient procedure for describing and calculating rectifier characteristics. However, a careful consideration of the number and form of the characteristic curves required to describe the more commonly used rectifier circuits and a study of the procedures involved in the use of these curves by the rectifier designer, indicate that the reactance factor as defined in the paper has a number of advantages over the other definitions which are possible.

If the reactance factor is defined by the quantity I_cX_c/E_s, a complete set of curve sheets will be required for each type of circuit, classifying the circuits in accordance with the value of P. While the angle of overlap can be shown on a single curve sheet for all types of circuits, if the reactance factor is defined by the full third term of the overlap equation, most of the other characteristics such as overlap factor, power factor, harmonic voltages and currents, interphase transformer voltage, and so forth, have values which are functions of both the angle of overlap and the number of phases P. Therefore, these characteristics require different curve sheets for the different values of P, regardless of the manner in which the reactance factor is defined. Furthermore, the reactance factor I_cX_c/E_s represents the simplest form in which the essential factors may be combined in a single quantity and should be easiest to use.

Thermal Co-ordination of Motors, Control, and Their Branch Circuits on Power Supplies of 600 Volts and Less

Discussion of paper 42-79 by B. W. Jones, presented at the AIEE North Eastern District meeting, Schenectady, N. Y., April 29–May 1, 1942, and published in AIEE TRANSACTIONS, 1942, July section, pages 483–7.

Jerome J. Taylor (The Detroit Edison Company, Detroit, Mich.): The approach to the subject of generalized protection of motors and lines is interesting and direct; the subject is a live one now, because many people do not realize how explosive a low-voltage fault can be when source capacity is large. In the latter respect ordinary cartridge fuses, properly constructed and installed, are remarkable both as to current-interrupting and current-limiting capabilities, with the result that in many cases they will clear the circuit with no disturbance whatever, regardless of prospective short-circuit current. This is somewhat more than can be said for most breakers in spite of the comparatively high cost of other types. The combination of such fuses with overload relays of the type described in the paper, appears to give complete protection to the equipment involved, and to the flexible in its application to individual requirements.

Selenium Rectifiers and Their Design

Discussion and author's closure of paper 42-86 by J. E. Yarmack, presented at the AIEE North Eastern District meeting, Schenectady, N. Y., April 29–May 1, 1942, and published in AIEE TRANSACTIONS, 1942, July section, pages 488–95.

C. G. Howard (Fansteel Metallurgical Corporation, North Chicago, Ill.): This paper is timely and one I consider highly beneficial to the members of AIEE engaged in the application or use of rectifiers.

The subject has been very well handled by Mr. Yarmack, who has been associated with the manufacture and application of the selenium rectifier since its introduction in the United States about four years ago.

I have closely followed the application and growth of the selenium rectifier in the electrical industry since its development in Europe about 1932. Having been associated with the manufacture of rectifiers since 1924 for various branches of the railway, electrical, and radio industries, I have been highly interested in basic developments of rectifiers of the electrolytic and dry disc types.

Our company's first application of the selenium rectifier was in the railway signal field, where it has been accepted by a large number of the railroads as a highly reliable means of charging signal batteries, and supplying d-c power for various signal and communication purposes.

Having studied and tested the selenium rectifier for about ten years, I am well acquainted with its characteristics and limits. To my knowledge, the selenium rectifier has been highly satisfactory in field performance wherever properly applied, and service conditions taken into account.

Selenium-rectifier equipment manufactured by our company, incorporating hundreds of thousands of selenium plates, has established field service records which substantiate the performance and life claims made for this rectifier.

I did not attend the meeting at which the paper of Mr. Yarmack was presented, so I am not familiar with any comments which may have been made from the floor regarding the selenium rectifier. I can only say that those who are interested in investigating the true properties and characteristics of the selenium rectifier, will find it to be a very useful development.

E. A. Harty (nonmember; General Electric Company, West Lynn, Mass.): The method used by the author to obtain the constants of the rectifier circuit, as well as those of the rectifier unit, appears very elaborate and less direct than the following procedure:

Inasmuch as the voltage impressed per element is quite definite and cannot be exceeded without danger of breaking down the blocking layer, there is a definite maximum average d-c voltage that can be obtained for each type of circuit at the maximum rated current.

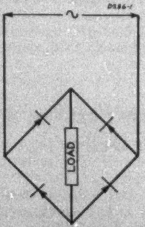


Figure 1. Full-wave single-phase bridge-type circuit

For example, for a single-phase full-wave bridge circuit such as shown in Figure 1 of this discussion, the d-c voltage at full load is 12 volts. Therefore, for every 12 volts required, one cell per leg would be needed.

A family of curves as shown on Figure 2 of this discussion permits estimating the a-c voltage required for each cell per leg, used in the proposed rectifier circuit. Values are shown at different current densities. All that is required is to multiply the readings obtained by the number of series cells used.

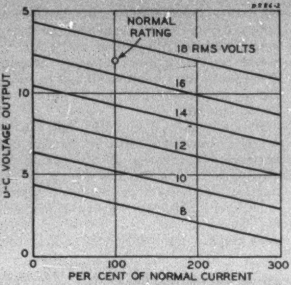


Figure 2. A-c–d-c voltage of unit bridge selenium full-wave rectifier (resistance load)

The author frankly admits that the selenium rectifier ages and that both the output and efficiency decreases with time. However, according to my records based on ten years' experience, the author's "dv" term increases more than 50 per cent. Aging data accumulated from foreign and domestic tests, by the writer, seem to indicate that all selenium rectifiers which were manufactured in Europe previous to 1938 aged very badly if operated at full voltage and that their resistance more than doubled. Typical aging curves are shown in Figure 3. Aging data on units manufactured since 1938 appear to show promise of better aging. Domestic cells are relatively new, and aging data do not exceed two years' duration, and only time will tell how they will age.

Inasmuch as not more than 18 volts rms can be safely applied to selenium elements a family of curves should be given similar to Figure 2, except using cells that have aged fully. To prevent overloading cells, this latter curve should be used for design purposes and not the curves for new elements, as recommended by the author.

Aging in rectifier circuits where the rectifier resistance changes with time can be minimized by making the rectifier resistance a small percentage of the total circuit resistance. Figure 4 shows this graphically. The use of fins, wider spacing, and fans results in making the rectifier resistance a larger portion of the circuit resistance. Assuming that the author's "dv" term, which is a function of the rectifier resistance, changes only 50 per cent by reference to Figure 4, the amount of aging can be anywhere from 5 to 22 per cent depending on whether the rectifier resistance is 10 or 50 per cent of the circuit resistance.

Hence, overrating of selenium rectifiers should be watched carefully. D-c voltages should always be derated, to prevent increasing the a-c voltage beyond the safe limits as specified by the manufacturers, which may result in rectifier failures.

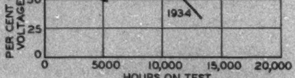


Figure 3. Aging of selenium rectifiers

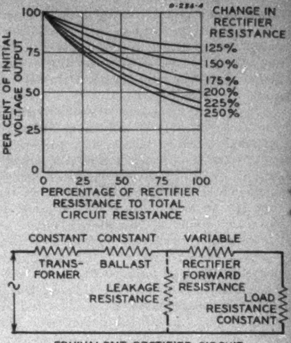


Figure 4

John E. Yarmack: Although Mr. Harty's suggested simplified method of design of selenium rectifiers would be useful in some cases, it would not, in the author's opinion, serve the purpose when one deals with a large variety of plates of different assemblies and sizes, and for all possible circuits, kinds of loading and types of service.

As to the aging of selenium rectifiers, the discusser illustrates a marked progress made in the technique of manufacture of selenium rectifiers. The illustrations and comments, however, lack information as to the extent of these tests and the conditions under which they were performed. It is worth reiterating, however, that not voltage per plate, but the current and the ultimate temperature of the plates created by current flowing, is the real factor causing higher or lesser degree of aging of selenium rectifiers.

Reactance and Skin Effect of Concentric Tubular Conductors

Discussion and author's closure of paper 42-78 by H. B. Dwight, presented at the AIEE North Eastern District meeting, Schenectady, N. Y., April 29–May 1, 1942, and published in AIEE TRANSACTIONS, 1942, July section pages 513–18.

H. W. Bousman (General Electric Company, Schenectady, N. Y.): Those of us who use concentric conductors will welcome this latest contribution to the design of such conductors. Some years ago Professor Dwight taught us to express the resistance ratio of an isolated tube in terms of the square root of the ratio of frequency to resistance per unit length. This parameter has proved to be a great timesaver for the designer who may have to deal with a variety of conductor materials. Even when copper is used, the parameter is convenient and useful, because it minimizes the computation required to show the effect of temperature on the resistance ratio. Would it be possible to put the resistance ratio of an outer return conductor in terms of this parameter?

Incidentally, there is a relation between the resistance ratio of an outer tube and that of the same tube used as an isolated conductor. The symbols are those of the paper.

$$\left(\frac{R_{ac}}{R_{dc}}\right)_{\substack{\text{as outer}\\ \text{conductor}}}-1=\left[\left(\frac{R_{ac}}{R_{dc}}\right)_{\substack{\text{isolated}\\ \text{tube}}}-1\right]\frac{r}{q}$$

This is a simple slide-rule procedure for design use in determining the resistance of outer tubular conductors from the author's previously published curves for isolated conductors.

H. B. Dwight: In reference to Mr. Bousman's discussion, the curves of Figure 2 may be plotted on mt where

$$m=\sqrt{\left(\frac{8\pi^2 f}{\rho}\right)}$$

as shown in equation 11. The quantity m is a constant, for Figure 2. One may write

$$mt=t\sqrt{\left\{\frac{8\pi f}{R(r^2-q^2)}\right\}} \qquad (1)$$

where R, which equals

$$\frac{\rho}{\pi(r^2-q^2)}$$

is in abohms per centimeter. By multiplying the horizontal readings for each curve in Figure 2 by $\sqrt{r^2-q^2}/t$ times a constant, one can plot the curves on $\sqrt{(f/R)}$, as was done in AIEE TRANSACTIONS, volume 41, 1922, page 189. If desired, curves plotted on mt may be accompanied by the equation

$$mt=0.0277\sqrt{\left\{\frac{tf}{R(d-t)}\right\}} \qquad (2)$$

as was done in the *General Electric Review*, April 1930, page 250, R being in ohms per 1,000 feet. Where the tubular conductors are stranded, it is undoubtedly preferable to use R, the resistance of the conductor for some assigned length, rather than ρ the resistivity.

All of the curve sheets based on R require a slide-rule operation before reading from the curves, but Figure 2 requires no slide-rule work, for solid copper tubes, unless one is using a different frequency or resistivity.

For a frequency f, multiply the tube thickness by $\sqrt{f/60}$ before reading from the curves, as stated after equation 29 in the paper.

For resistivity ρ_1, multiply the tube thickness by $\sqrt{\rho/\rho_1}$ before reading from the curves, where ρ is the resistivity of standard annealed copper at 75 degrees centigrade, in the same units as ρ_1. In this way, changes in resistivity caused by the metal used or the temperature, may be allowed for.

The stranding of tubular conductors has often been a source of misunderstanding. Such conductors are really a mixture of metal and air, and ρ_1 for a stranded conductor should be obtained by dividing R, the resistance of the conductor per unit length (one centimeter or one inch), by $\pi(r^2-q^2)$, the gross area of cross section (square centimeters or square inches).

The equation given in Mr. Bousman's discussion should be useful at low frequencies, for the range of Figure 2, that is, for the resistance ratio of the outer tube less than about 3. It can be derived by omitting the later terms of the low-frequency solution, and it can be checked by readings from Figure 2. For instance, for $t=0.9$ inch and $t/d=0.20$,

$$\frac{R_{ac}-R_{dc}}{R_{dc}}\text{ (outer)}\div\frac{R_{ac}-R_{dc}}{R_{dc}}\text{ (isolated)}$$

$$=\frac{1.80}{1.10}=1.64\text{, from Figure 2}$$

or

$$=\frac{r}{q}=\frac{1}{0.6}=1.67$$

by Mr. Bousman's equation.

For frequencies high enough for the later terms of equations 36 and 37 to be discarded, a different result is obtained, namely,

$$\frac{R_{ac}\text{(outer)}}{R_{ac}\text{(isolated)}}=\frac{r}{q}$$

However, this has no apparent application, for the natural procedure would be to use the high-frequency formulas directly.

Temperature-Aging Tests on Class-A-Insulated Fractional-Horsepower Motor Stators

Discussion of paper 42-82 by J. A. Scott and B. H. Thompson, presented at the AIEE North Eastern District meeting, Schenectady, N. Y., April 29–May 1, 1942, and published in AIEE TRANSACTIONS, 1942, July section, pages 499–501.

Jerome J. Taylor (Detroit Edison Company, Detroit, Mich.): The paper is a useful addition to the knowledge of class A materials, which (latter) are a large proportion of all insulation now in use. With wartime conditions exerting pressure toward increased loading of equipment, such knowledge is of very great value. Nevertheless, it is to be hoped that eventually a low-cost insulating material can be developed that will operate indefinitely at high temperatures without deteriorating. The temperature-sensitivity of most present insulations, compared to copper and steel, has for years past forced many designs and ratings to be based on this one factor alone. There is now reason to think that the severity of the restriction will soon be eased. When this is accomplished, ratings can be based on other factors such as efficiency, voltage drop, torque, and so forth, which have a more logical relation to performance, and the field will then be open to new and interesting designs.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): The authors are to be congratulated on the practical method of making tests on small motors. As pointed out in the paper, their findings of degrees' increase to half the life is in fair agreement with what has been found for class A insulation used in transformers. If it were practical to make similar tests on transformers, we might find that the results would not check the 8-degree rule, which I have used for several years, as well as Scott and Thompson found, namely, a 10.5-degree, 11-degree rule and, in one case, a 15-degree rule. The reason for wide variations found in the length of life is given in the following statement which appeared in a paper entitled "Emergency Overloading of Air-Cooled Oil-Immersed Power Transformers by Hot-Spot Temperatures" presented at the summer convention:[1]

> "The estimated life of a transformer is at best only a rough approximation. The dielectric strength of insulation does not deteriorate until it has become embrittled and cracked. It is possible therefore for a transformer to continue to operate long after the mechanical life of its insulation is well used up unless subjected to excessive mechanical stresses (short circuit, handling, or other mechanical shocks), since its normal stresses are quite low. It does not follow therefore that a transformer will fail when its insulation is embrittled; on the contrary, if severely strained it might fail before the mechanical strength is completely used up."

While no one can claim to predict the exact life of apparatus by any rule, we are making progress. I feel that the authors of this paper have made a very worth-while contribution to this complex problem.

REFERENCE

1. EMERGENCY OVERLOADING OF AIR-COOLED OIL-IMMERSED POWER TRANSFORMERS BY HOT-SPOT TEMPERATURES, V. M. Montsinger, P. M. Ketchum. AIEE TRANSACTIONS, volume 61, 1942, pages 907–16.

High-Voltage Fusing of Transformer Banks

Discussion and authors' closure of paper 42-80 by H. H. Marsh, Jr., and G. B. Dodds, presented at the AIEE North Eastern District meeting, Schenectady, N. Y., April 29–May 1, 1942, and published in AIEE TRANSACTIONS, 1942, July section, pages 533–5.

Bruce O. Watkins (nonmember, Rural Electrification Administration, St. Louis, Mo.): It is gratifying to see that the authors give consideration to the setting and the type of the protective device on the load side of the transformer bank before deciding on a fuse for the supply side. On Rural Electrification Administration systems it usually is necessary to install as high a rating of device as possible, consistent with short-circuit protection of the transformers on the load side, in order to obtain a sufficient number of co-ordinated overcurrent devices in series for sectionalizing the rather lengthy lines. Often severe limitations in sectionalizing the system have been imposed by the supplying utilities' requirements for the supply side fuse.

Some operators also do not seem to realize that the time-current characteristics of fuse links of the same rating, but made by different manufacturers, are totally different. It is entirely insufficient to specify the fuse rating, and probably the better method is to define the required time-current characteristic.

It is to be hoped that other utilities will follow this lead, and that the practice of fusing for a specified time full-load transformer rating will soon become obsolete.

H. L. Rawlins (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): This paper is a very welcome one in that it recites the experience and practices with high-voltage fuses. There has been a dearth of information dealing with high-voltage fuses from the operators' viewpoint, and it is hoped that this paper will open the field for further discussions.

The direct comparison between fused stations and unfused stations is very enlightening, and vividly reveals the ability of high-voltage fuses to rapidly clear a fault before serious damage can occur or the fault spread to adjacent apparatus.

The authors have stressed the use of 34.5-kv fuses on a 22-kv system. This practice may have been desirable at one time and no doubt was adopted before the advent of laboratory tested fuses of high interrupting ability. The requirement of the company with which I am associated is that high-voltage fuses must satisfactorily interrupt all values of current from the minimum current that will melt the link to currents in excess of the interrupting rating, with rated voltage across one unit, on circuits having a rapid rise of transient recovery voltage. Many years of satisfactory operating experience with high-voltage fuses meeting this requirement indicate that overvoltage fusing is not justified. Furthermore, certain types of fuses, when of higher-voltage rating than required, may, during interruption, produce voltage surges in excess of the insulation strength of the system.

Of particular interest are the authors' remarks concerning proper maintenance of high-voltage fuses. Such fuses, protecting transformer banks, when properly applied with respect to secondary protective devices, will never blow unless there is a fault within or immediately adjacent to the transformer. It is conceivable that such a fuse could be in service 20 years or more before being called upon to interrupt a fault. It is apparent, therefore, that a definite program for the inspection and maintenance of high-voltage fuses is required, and it would be interesting to know the requirements set up by the Duquesne Light Company.

H. H. Marsh, Jr., and G. B. Dodds: In Mr. Rawlins' comments of this paper he has brought up two points that make further explanation by the authors desirable. The first of these is his question as to our reasons for using 34½-kv fuses on a 22-kv system. As Mr. Rawlins surmised, this practice was adopted in the early days when original spacings and construction of fuses and mountings were found to be inadequate. As pointed out in the paper it was found that the higher-voltage rating fuses cured some of the early difficulties.

Since that time the manufacturers have developed and tested their fuses to the point where reliable and satisfactory operation of fuses at their nominal rated voltage is satisfactory. In the meantime, however, we had installed a considerable number of the fuses of a higher-voltage rating, and we have continued that practice in the Schweitzer and Conrad type *D* and *C* fuses, in order to simplify the replacement and the storeroom stock of fuses. In the use of fuses on other voltages and in the use of fuses of other types, where the usage commenced after satisfactory fuses of nominal system voltage rating were available, we have not continued the practice of using overvoltage fuses.

In the second point brought up by Mr. Rawlins in which he called attention to the fact that fuses may be in service for long periods of time and not be called upon to interrupt a fault, he recognizes that it is difficult to test the condition of such fuses and invites information as to what practice is followed on the Duquesne Light Company system.

As the Schweitzer and Conrad liquid-type fuse is the only fuse we have had in use for a sufficiently long period of time to obtain worth-while experience, our comments will be restricted to this type of fuse. Defects that have been located by observations are low liquid, cracks in the glass tube, and discoloration of the liquid. In the event of the first two of these items, the fuse should obviously be replaced. In connection with the discoloration of the liquid, experience must indicate whether this is a normal or abnormal discoloration. Normal discoloration consists of the liquid assuming a slightly brownish tinge but remaining clear, while abnormal discoloration may cause a milky appearance or a precipitate. In some cases of improper sealing, water may have gained access to the liquid.

In regard to other types of fuses, our experience has been limited either to too few a number or too short a period to be of any value in determining whether the fuses are in good operating condition or not. We would welcome advice from the manufacturers of the Schweitzer and Conrad type *SM* and the Westinghouse *BA* fuses as to how we could determine whether such fuses are in satisfactory operating condition.

Sleet Problems on Electrified Railroads

Discussion and author's closure of paper 42-121 by H. F. Brown, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, August section, pages 589–93.

F. E. Snell (Cleveland Railway Company, Cleveland, Ohio): The problem of sleet prevention in the light traction industry came into existence simultaneously with installation of the first trolley wires strung in localities subjected to freezing weather. Because of geographical location, some cities are naturally more liable to sleet storm damage than others which are situated in the more temperate zones. For this reason the methods used in combating sleet may differ greatly in different sections of the country.

During the last 50 years many devices have been used with varying degrees of success for effective removal of sleet from trolley wires. Some of these devices were soon discarded, because the trolley wire was greatly damaged by their use.

The trolley wheel, used extensively on street cars, has an average life of approximately 12,000 miles. In spite of this average life, a single bad sleet storm may wear out many wheels which are almost new. Carbon shoes, used on modern street cars and trackless trolley coaches as current collectors, are critically affected by sleet, and much trouble is experienced under such circumstances.

In addition to trolley wheel and shoe damage, trolley wires may also be damaged to such an extent that several wire failures occur during a prolonged severe storm. Sleet, accompanied by high winds, may cause considerable damage to overhead plant as well as costly service interruptions.

Without doubt, preparedness is the most important ally of a traction company in combating sleet. Some sections of the country are very susceptible to sudden drops in temperature. Should this occur during a rain storm and at a temperature slightly above the freezing point, formation of ice on trolley wires is a logical result. Any operating difficulties which may develop, however, depend not only upon the severity of the storm but also upon the preparedness of a company promptly to remove ice as it forms. Experience proves that street-car or trackless-trolley overhead can be kept operative as long as vehicles can move in the street.

Contrary to popular belief, neither expensive equipment nor an abnormal organization are necessary to keep overhead plant relatively free from ice during a freezing storm. The one important fact is to know what is coming and be prepared for it. When weather changes may be expected, a short-wave radio tuned to pick up aviation weather reports at stated intervals of time is of great assistance. This, accompanied by a check with the local weather bureau, will permit an operator to foresee the possibility of sleet by several hours. If it is raining when the temperature is slightly above 32 degrees, frequent thermometer readings will indicate any change toward the freezing point.

Co-ordination of all available weather information is of great importance. This work is often one of the duties of a trouble dispatcher, or it may be assigned to some other authorized employee who is capable of determining the probable condition of the weather. When ice threatens to damage property and disrupt service, it is his responsibility to notify the proper officials as far in advance as possible.

At such a time service on overhead plant should be increased as much as possible, because the frequent passage of trolley wheels or shoes will prevent minor ice formation. If conditions become worse, carbon shoes should be removed from the vehicle and replaced with old steel shoes which have grooves worn into their faces. The relatively soft carbon has very little cutting effect and is conducive to severe arcing, whereas the action of the steel shoes is much more pronounced. Frequent service on all wire, accompanied by the use of steel shoes on those vehicles normally equipped for shoe operation, will permit satisfactory operation during average storms.

In extremely severe cases "sleet cutters" should be used. Such a cutter may be attached to the standard trolley wheel for street-car operation, but in the case of trolley coaches the sleet shoes should be substituted for steel shoes. A minimum number of coaches should be provided with sleet shoes at first and the number increased, if necessary, dependent upon the severity of the storm.

A severe storm can also be combated by the use of a line truck. The truck should be equipped with two old trolley bases and two trolley poles having rigid harps and sleet-cutting wheels should be available for instant use. When in service the base spring tension should be set to approximately 40 pounds. This excessive force of the wheels against the wires will effectively remove a heavy coating of ice. As soon as a storm subsides or ice ceases to form, all cutting shoes or wheels should be removed immediately, because they are liable to score the wire if used excessively.

Necessary investment in special equipment is surprisingly low and does not require an increased maintenance personnel.

In closing, two facts are of prime importance and they must never be forgotten or overlooked. Know what is coming and be prepared when it arrives. Experience has often proved that it is easier and cheaper to *keep* out of trouble than it is to *get* out of trouble.

D. R. MacLeod (General Electric Company, Erie, Pa.): Sleet storms can cause serious interruptions in service on electrified railroads. Fortunately, the type of sleet storm which causes interference with electric operation is not of frequent occurrence, but, since it is the aim of the railroads and rapid transit systems to maintain their schedules under all conditions, the operators make provision for this type of emergency also.

One of the difficult problems is that of cars and locomotives lying in yards over night with their pantographs down. Ice may form to such a thickness as to prevent the raising of the pantographs. It would be interesting to know what methods are used to insure getting them in service promptly during sleet storms. When the pantograph has been raised, electric methods of heating provide the best-known means of keeping the pantographs free from ice. The equipment required on an a-c locomotive or car is relatively inexpensive and can be installed in the space available at the base of the pantograph. The equipment must be designed to withstand normal and transient voltages on the trolley and should be protected against faults to ground at the pantograph.

Various methods of using load currents to keep trolley wires free from ice are available. Usually these methods are at the expense of regulation, so that low voltages must be tolerated during the emergency. In this paper mention is made of three schemes one of which has been used for some years. The water-box scheme is an emergency method which can be justified on the basis that it requires an inexpensive equipment. The scheme shown in Figure 3 (scheme 1) of the paper is subject to the disadvantage that care must be taken to prevent disastrous overvoltages in case a ground occurs on one of the trolleys to which the 1,000-volt secondary winding of the heating transformer is connected. A ground on the trolleys is equivalent to a short circuit on the heating transformer. A circuit breaker should be provided on the 1,000-volt side and interlocked with the 11,000-volt breaker so that the latter can not open before the 1,000-volt side is cleared. The 1,000-volt winding must be designed to stand 11,000 volts. Another disadvantage of scheme 1 is that it maintains 1,000 volts between trolleys which could not be permitted where there are crossovers between the tracks.

W. J. Clardy (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): It is mentioned in Mr. Brown's paper that the "dancing" of conductors has been observed when no sleet or wind are present. In some instances, such movement occurs during rapid intensity changes in the magnetic fields surrounding the conductors. These sudden alterations set up repulsions and attractions between cables which cause the oscillations. Any quick current build-up or fade-out produced by circuit interruptions incident to sleet on the contact wire or due to other rapid variations in power demand may lead to the phenomena.

The suggested scheme of using 11,000-volt energy passing through a trolley or feeder circuit and a water rheostat to ground, for heating the wire, appears wasteful of power. A current flow of 800 amperes is 8,800 kw at 11,000 volts and unity power factor. This is 8,800 kilowatt-hours per hour. Thus, at one cent per kilowatt-hour, the operating expense is 88 dollars per hour for each trolley or feeder circuit and some 95 per cent of this amount is wasted in resistor losses.

No mention is made of the measures which may be undertaken to avoid difficulties because of sleet or third rails. This is a problem which requires attention on suburban electrifications and urban rapid transit systems. The Chicago, Aurora and Elgin Railroad has a unique device for freeing the third rail of sleet. It consists of a special six-inch shoe which is moved down in contact with the third rail in advance of the collector shoe. Actuation is by means of a four-inch cylinder supplied with air at 100 pounds pressure. The shoe has seven blunt nose teeth which act as cutters. The teeth are set at a 45-degree angle so as to be self-freeing.

Dwight L. Smith (Chicago Rapid Transit Company, Chicago, Ill.): Mr. Brown's excellent paper deals with the prevention and removal of sleet on overhead trolley only and does not cover the subject so far as lines operating on third rail. The Chicago Rapid Transit Company operates almost entirely on top-contact unprotected third rail and faces a potentially serious sleet problem because of this type of construction. It would be desirable to either convert to under contact rail or to protect the present top contact rail, but both structural and equipment clearances are such that the cost of either of these plans would be prohibitive. Most of the third rail on the elevated lines is 80 pounds with a conductivity equivalent to approximately 1,000,000 centimeters of copper and the third rail to be used in the subway is 144 pounds, special section low carbon with a conductivity equivalent to over 2,000,000 centimeters. With such conductivities, it is manifestly impossible to handle the sleet problem by resistance heating as may be done with overhead systems.

The Chicago Rapid Transit Company has solved the problem by the use of mechanical sleet crushers and scrapers on motor cars. These devices are installed on the beam supporting the trolley shoe collector and are normally held from contact with third rail by cams. When their use is necessary, they are lowered to make contact with the top surface of the rail and are forced down by spring pressure of about 150 pounds. The crusher is a cylinder approximately three inches in diameter with helical ridges which rolls and breaks up and crushes the sleet coating. The scraper consists of two hardened steel blades arranged at an angle of about 30 degrees with the line of the trolley rail, which follow the crushers and scrape off the crushed ice. Formerly these devices were used only for sleet removal and the ordinary shoe collector relied on for current collection; later the scraper was connected in parallel with the collecting shoe and thus itself acts as a current collector.

Naturally, considerable arcing occurs because of imperfect contact, but this has been prevented from jumping to grounded portions of the truck by the use of flashboards and cup washers on the bolt ends. When the type of construction makes it possible, the wooden shoe beam is constructed with its top face at about a 45-degree angle in order to prevent sleet and moisture from collecting on a horizontal surface.

With this device it is possible to remove sleet which has accumulated on the third rail, and the operation of a fairly frequent service prevents further accumulation.

At certain critical locations such as on inclines where the sleet tends to flow downgrade and results in a very heavy coating near the bottom of the grade and in yards where cars have been stored during a sleet storm without any movement on the track, special means are used to remove and prevent further accumulation of sleet. The company has developed a sleet paste which consists of a mixture of powdered graphite, an antifreeze vehicle and a small amount of light oil mixed to the consistency of a thin paste. This paste is applied by hand by means of a long-handled brush and disintegrates heavy sleet coatings rapidly and prevents further sleet from adhering to the rail for an appreciable time. No successful means have been found for applying it other than by hand, and the cost of the paste itself and its application has resulted in confining its use only to such critical locations.

By the two means described above, delays due to sleet accumulation on the third rail have been almost entirely eliminated.

H. F. Brown: Mr. Smith's comments relative to sleet removal from third rail are interesting and timely, in connection with this general subject. This particular phase of the sleet problem was not touched upon in my paper as being outside its immediate scope. The New Haven Railroad does not operate or maintain any third rail, although their locomotives operate into the Grand Central Station over the New York Central tracks, from under running third rail.

Mr. MacLeod touches upon some design difficulties of scheme 1, outlined in the paper. These were recognized but not detailed in the paper, as this scheme was not used.

Mr. Clardy points out the economic disadvantage of the water-box scheme. Such costs as he mentions must be weighed against the possibility of operation by other forms of motive power and against the cost of delays to important passenger trains. His comments regarding conductor movements (dancing) due to sudden alterations in their surrounding field are important in connection with this subject, since this phenomenon has been often noticed on the feeders mentioned in this paper.

Electric Control for Steam Boilers on Diesel-Electric and Straight Electric Locomotives

Discussion and author's closure of paper 42-127 by E. H. Burgess, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942 and published in AIEE TRANSACTIONS, 1942, August section, pages 604–06.

Arthur Bessey Smith (Associated Electric Laboratories, Inc., Chicago, Ill.): This paper is a welcome addition to the literature of electric control, whether for boilers, machines, or other devices.

The necessary brevity of the paper left numerous points open to question, some of which I believe should be added to complete the picture.

1. It seems to be assumed that the heat generated by the burner varies with the speed of the burner motor.

2. As the boiler pressure rises, it seems that the pressure switch *opens* the contacts in the sequence 5-4-3-2-1.

3. It is not clear how the coils of the pilot relay and the solenoid valve are connected to the heavy duty contacts. If wired exactly as shown, when high pressure has opened all of the contacts, current will be cut off both pilot relay and solenoid; thus both will release.

4. Does the pilot-relay contact close at the operation or at the release of the pilot relay? This is involved in the pilot relay interlock during the off period, when the time-delay relay is said to be kept energized, page 604, column 3 of the paper.

5. How is the water level in the drum or equivalent maintained?

6. What is the function of the coil marked "pressure switch?"

7. On page 604, column 2, "boiler is ready to run"—does this mean "ready to raise steam from cold?"

E. H. Burgess: 1. Heat generated by the burner varies with the speed of the burner motor inasmuch as the amount of fuel forced through the burner is proportionate to the burner motor speed.

2. When the pressure switch coil is energized, the contact fingers close in sequence 1-2-3-4-5, which actually provides step starting for the motor. When the steam pressure rises to a predetermined point number 5 finger opens, thereby inserting a definite amount of resistance in series with the motor through the number 5 load relay. As the pressure continues to rise, the balance of contacts open in sequence 4-3-2-1, and after number 1 opens, the motor stops entirely.

3-4. The pilot relay coil and solenoid valve coil are connected to the armature of the number 1 and number 2 load relays and are only energized when either of these load relay contacts are closed, when high pressure causes all the heavy duty relay contacts to open, then both the solenoid valve and pilot relay became de-energized. The contacts of the pilot relay make only when the coil is de-energized, which, as can be seen from the circuit, complete the circuit to the coil of the outfire relay.

5. During the normal operation of the boiler an excessive amount of water is pumped into the coils, which does not turn into steam. This excess water is removed from the steam line in the separator, and the height of water in the separator is controlled by a ball float. The excess water then returns to the storage tank through a heat exchanger.

6. The multifinger pressure switch is of the normally open contact type which, when the coil is energized, slowly closes the contacts in sequence 1-2-3-4-5. Steam pressure exerts pressure against the contacts only and in no way interferes with the operating coil.

7. "When boiler is ready to run," means that all operating and safety devices are set in normal position, such as main line switch closed, all safety switches closed, and ignition switch in the "on" position. The preceding conditions must be met before the boiler will generate steam, regardless of whether it is from a cold start or from a hot boiler that has just been shut down.

Electric Facilities and Operating Plan for the First Chicago Subway

Discussion of paper 42-137 by C. E. De Leuw, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, November section, pages 780–7.

W. J. Clardy (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Mr. De Leuw's interesting paper gives a comprehensive outline of progress in the betterment of urban travel facilities in Chicago. Rapid transit is essential to the prosperity and growth of every large city. Subway and elevated lines serve as means of low-cost and convenient access to shopping and delivery districts. The territory available for residential purposes is increased, and the concentration of workers in homes near offices is avoided.

The expansion of New York, Chicago, Philadelphia, Boston, and Cleveland demonstrates this dependence on quick travel between various sections of cities. During recent years many additions have been made to the subway lines which serve New York. An interesting phase of the development is the replacement of elevated lines in a number of locations. After new subways were completed and operation initiated, the old elevated routes were abandoned and the structures demolished. Thus, street appearance is improved, and property values bettered. The New York system has introduced some novel types of subway streamlined rolling stock. A multisection car, composed of five bodies on six trucks, is in operation which seats 198 passengers and has a total capacity of 712. The weight of this unit is 182,600 pounds. Another articulated car used consists of three bodies on four trucks. There are 84 seats, the total capacity is 318, and the weight 75,540.

Rapid transit in Chicago dates back to 1892. The present elevated lines are functioning remarkably well within their limitations, but the city requires additional facilities to provide for growth and expansion. The new subway will contribute materially in meeting these needs. War conditions are preventing the immediate purchase of new rolling stock for the subway, but reasonably satisfactory operation can be expected from existing steel cars of the Chicago Rapid Transit Company. The delay in the procurement will prove beneficial because of the increase in production capacity of lightweight materials suitable for car construction after the war.

Relative Expense for Service Restoration With Different Types of Overcurrent Protection for Distribution Circuits

Discussion and authors' closure of paper 42-129 by G. F. Lincks and C. R. Craig, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, November section, pages 813–21.

E. L. Bayles (Northwestern Public Service Company, Huron, S. D.): The Huron–Iroquois line of the Northwestern Public Service Company is a three-phase 11-kv line approximately $48^{1}/_{2}$ miles long with solidly grounded neutral, using number 6 hard-drawn conductor. Thirty-foot poles are used with triangular construction having a $4^{1}/_{2}$-foot crossarm, a forged steel pole-top pin, and 28-inch strap braces. Steel pins are used in the arms, and insulators are nominally 15 kv. A one-quarter-inch Siemens–Martin steel neutral wire is carried the entire length of the circuit on the poles at approximately one foot below the lower attachment point of the braces.

The neutral wire is grounded at each

transformer installation, at each overhead guy and each pole. Grounding conditions are poor to fair. Driven ground rods on transformer installations and down guys are connected to the neutral test from eight ohms on up, while pole grounds run from a minimum of 100 ohms on up to an unreadable value.

The terrain is quite flat with maximum exposure of line to lightning, wind, and sleet. There are practically no trees or other objects which might shield the line, and by the same token faults are not caused by proximity to trees.

Transformers fed from the line are protected by expulsion gap arresters or by standard valve-type arresters. There are, however, a number of older types of transformers which have a very low impulse strength and which flash over internally in some instances at a lower voltage than the settings of the arresters of either type. These transformers are being weeded out as time goes on but are the probable source of some few kickouts on this line apart from the trouble experienced on the line itself. Inasmuch as the transformer fusing is interposed between the line and transformer troubles, this is largely a matter of co-ordination which in most cases was such as to relieve the line by blowing the transformer fuse.

This line normally serves five small towns and a good-sized packing house load which taps off at a point a few miles from the source. A 15-kv reclosing oil circuit breaker with time-over current relays protects the entire line while *three* F.P. 119 oil reclosers are in the line to the small towns at the point just beyond the packing-plant tap and thus protect this important load from trouble on the 40 odd miles of line. The oil circuit breaker is set at 48 amperes with a time of one second at 300 per cent of trip current. The F.P. 119 oil reclosers are the 25-ampere size.

The oil reclosers were installed May 15, 1941, and replaced a three-shot reclosing fuse installation. For the 12-month period ending May 15, 1942, the number of operations recorded by the reclosers were as follows:

Phase *A*	149
Phase *B*	36
Phase *C*	172

Total number of lockouts	4
Permanent line faults	2
Lockouts on repeated transient faults	2
Faults cleared successfully	over 168

In explanation of the unusual number of operations it should be said that the period included a rather severe sleet or glaze storm with high winds which resulted in dancing conductors and numerous transient faults. On a similar installation near Watertown under sleet storm conditions we have experienced 27 recloser operations over night without a lockout.

It could not be said in fairness that the installation of these reclosers saved the replacing of over 350 fuses, as no doubt the time during which the fuse was out would include a number of potential cases of trouble. Unfortunately, we do not have an accurate record of fuse replacements prior to the installation of reclosers. We do know, however, that during one 24-hour period in a sleet storm we replaced 100 links in a three-shot fuse at the same point.

From the economic viewpoint we feel that time, material, car mileage, and outages saved by this installation more than justify the installation of this set of oil reclosers.

Bruce O. Watkins (nonmember; Rural Electrification Administration, St. Louis, Mo.): In the paper Mr. Lincks and Mr. Craig assume that there is a substation attendant. Usually on Rural Electrification Administration systems there is no such attendant. As is pointed out, this will result in greater relative benefits for automatic resetting reclosers. Also, it is assumed that in all cases there is a substation breaker. In very few cases are there such breakers on REA systems. A great many resetting reclosers can be purchased for the cost of one substation breaker, and the tendency in REA systems has been to place such reclosers on the line, rather than investing in a substation breaker. Also, since most REA substations are of relatively low capacity, the interrupting rating required of the substation devices has not usually necessitated such a breaker.

The experience with resetting reclosers on REA systems has been very favorable. Managers who have used them report greatly reduced outage time and maintenance costs over the originally fused system. Unfortunately, the exact reduction in such costs is not available. We are now attempting to compare maintenance costs on recloser-equipped systems with fused systems similar in size, consumer density, revenue, and so forth. Such figures may assist in sectionalizing comparisons.

Table I shows a three months' summary of a report on single-pole 15-kv resetting reclosers on REA systems which reported the same number of reclosers for three consecutive months (July, August, and September) in 1941.

There are features of the presently available resetting reclosers which could be improved. The ideal recloser would possess characteristics such that:

1. Internal distribution transformer fuses beyond the recloser would blow before recloser lockout on transformer failure.
2. A larger number could be co-ordinated in series than with present reclosers.
3. Opening time would be sufficiently fast to prevent excess conductor or gap burning.
4. Opening time would be short enough to protect adequately small substation transformers with the larger sizes of reclosers.
5. The installation and co-ordination of less expensive devices beyond the recloser could be made.

Of course some of these objectives conflict, and some compromise will be necessary. We feel, however, that the automatic resetting recloser offers great possibilities in rural distribution sectionalizing and that, as developments continue, they will offer even greater benefits.

Table I. Quarterly Summary

Number of systems reporting	39
Number of reclosers	450
Miles behind reclosers	11,441
Miles per recloser	25.4
Total operations	8,094
Operations per recloser	18
Lockouts	208
Lockouts per recloser	0.462
Per cent lockouts per operation	2.57
Operations per mile	0.706

R. F. Quinn (General Electric Company, Schenectady, N. Y.): The authors have presented a means of placing the problem of whether to sectionalize and with what to sectionalize on a sound economic basis. This study and the conclusions should be of real value to distribution engineers in studying their particular systems with a view to getting the maximum in continuity of service for every dollar invested.

The paper covers all combinations of sectionalizing and branch-protection schemes now available or in common use. However, manual sectionalizing is covered only in general terms. Actual data showing the effect on restoration time and automotive mileage for a line equipped with manual sectionalizing are not included in the curves.

Manual sectionalizing, as commonly employed, splits the main line up into several sections. In locating a fault the trouble crew travels at a high rate of speed to the mid-sectionalizing point, opens it, and then has the station operator close the circuit. If the line remains energized, the fault is obviously beyond the section point and vice versa. They then split the faulted half of the line into smaller sections until they locate the one on which the trouble is located. After locating the faulted section, the crew travels at a slower rate of speed to actually find and repair the fault.

An additional study has been made to arrive at comparable data on manual sectionalizing of a line without branches as in Figure 2 of the paper. Three locations of disconnecting cutouts having solid blades permitted dividing the line into four equal sections. The same general assumptions were used as in the study described in the paper, and it was assumed that the trouble crew:

(*a*). Traveled 30 miles per hour average speed to mid-point, opened cutout, phoned, and had breaker closed, and then either traveled back to the sectionalizing cutout closer to the substation if service was *not* restored, or to the cutout beyond the midpoint if service *was* restored (average speed 30 miles per hour). Climbed pole, opened cutout, and checked to see if service was restored. Then searched faulted section at 15 miles per hour

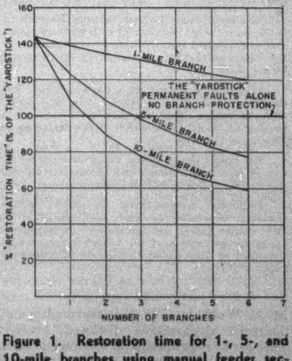


Figure 1. Restoration time for 1-, 5-, and 10-mile branches using manual feeder sectionalizing

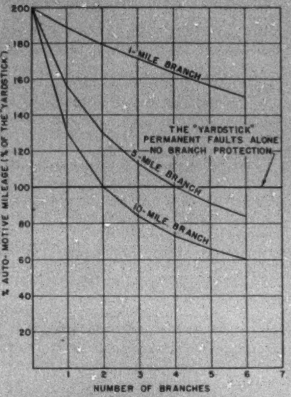


Figure 2. Automotive mileage for 1-, 5-, and 10-mile branches using manual feeder sectionalizing

traveling to mid-point. After repairing faults, traveled back 30 miles per hour to close sectionalizing cutout.

(*b*). With branches added to the feeder studied as in (*a*), search for the fault was confined to one-fourth the total mileage in all the branches. The crew traveled at 15 miles per hour.

(*c*). Five minutes were allowed for climbing the pole and 30 minutes for repairing permanent faults, which are the same values used in the study made by the authors.

The data obtained are plotted in Figures 1 and 2 of this discussion. It should be noted that the values are the same for all percentages of temporary faults. Thus, to make a comparison with the data presented by the authors, the curves on manual sectionalizing Figures 1 and 2 can be plotted directly on the respective curves Figures 5 to 12 of the authors' paper.

These data support the authors' conclusions on manual sectionalizing. They indicate that:

1. Manual sectionalizing of a feeder having no branches increases "restoration time" and automotive mileage in providing an improvement in service continuity over that for an unsectionalized line.
2. The restoration time and automotive mileage is the same for all percentages of temporary faults, and thus manual sectionalizing is most effective at the higher percentages of temporary faults (above 75 per cent).
3. Manually sectionalizing a feeder without any branches is much less beneficial than on a feeder with branches. Traveling out and back on a number of branches in search of the fault can be eliminated thereby.
4. The longer the branches, the greater the reduction of "restoration time" and automotive mileage effected by manually sectionalizing a feeder.
5. Manually sectionalizing a feeder having branches generally provides less reduction of "restoration time" and automotive mileage than automatic protection of the branches except with a number of branches at 85 per cent of temporary faults or higher.

M. C. Westrate (The Commonwealth and Southern Corporation, Jackson, Mich.): This paper is primarily an economical consideration of the fusing problems on distribution circuits and as such makes a logical engineering presentation of the problem. In the conclusions much emphasis is placed on the value of "overlapping the substation reclosing protection with all line protective devices," but this is not always possible, because, in many cases where long feeders are involved, a far end fault may produce less current than the full load current on the feeder at the substation. Also, if such a scheme is to be used, it would seem desirable to have the breaker clearing time below the damaging time for the various line fuses.

The conclusion that "single-element fusing of branches five miles and longer" is economically justified appears reasonable for the general case. However, severe tree exposures often make fusing practical on much shorter branches and possibly the criteria for branch fusing might be based on the number of faults occurring per year rather than length based on an average number of faults per year.

G. F. Lincks and **C. R. Craig:** The contributions made by those discussing the paper are greatly appreciated by the authors.

E. M. Bayles gives some interesting data on a line where the percentage of temporary faults are much higher than the 85 per cent maximum of the study and there are more faults. (4+ instead of 1 per mile.) These faults apparently occur largely during periods of severe lightning and sleet storms with a majority resulting from a very few storms. All of these special conditions would enhance the economic justification for using automatic resetting reclosers of the type mentioned.

In calling attention to the inability to always set substation breaker relays so as to clear ground faults at the end of the line, W. C. Westrate places emphasis on a limitation to the more widespread use of relaying so the substation reclosing protection overlaps all line protective devices. This limitation is more pronounced on ungrounded circuits than on grounded circuits where the ground relay can be set below the load current of the phase wires. Recognizing this condition, the study presented in the paper pointed out that where such overlapping protection reaches only part way out on the line, the decrease in "restoration time" and automotive mileage over a nonreclosing or reclosing breaker is only approximately one-quarter of the decrease provided by overlapping the whole line. A very fine presentation of the problems involved in this type of overlapping protection will be found in the engineering report 47 Edison Electric Institute publication *J*-1, "Positive Disconnection of Distribution Circuits During Faults to Ground."

In concluding that "single-element fusing of branches five miles and longer is the most effective in saving sufficient restoration expense to liquidate the initial cost," it was not intended to imply that this would never be true on shorter branches. As Mr. Westrate points out, conditions on specific circuits or branches of a circuit may require a different criterion than the length of the branch. In some instances a large number of faults per year or a higher percentage of temporary faults on a very short branch might justify the initial expense of single-element fuses or even reclosers by the savings in restoration expense as well as the improvement in service continuity thus provided.

The emphasis placed on the use of relays and substation breakers which overlap the line protective equipment may have misled Mr. Watkins into the interpretation that the study presented was based primarily on the use of breakers at the substation. Actually, with an attended substation, it makes no difference in the conclusions reached in the paper whether a relay and breaker, a recloser, or a fuse is employed. With an unattended station a reclosing device that reset automatically very likely would be justified by the saving in restoration expense. As Mr. Watkins indicates, the decision as to whether this device is a recloser or a relay and breaker depends on the economies of each circuit.

The operating records with reclosers which REA are compiling should prove of value to the industry. It has been suggested that the value of such records would be enhanced greatly if there were some standards set up for compiling the records. For example, in the three months operating data presented by Mr. Watkins, there is no segregation as to whether valve-type arresters, expulsion protector tubes, or plain rod gaps were employed. If the expulsion protector tubes are not properly co-ordinated with the reclosers, they may cause it to operate unnecessarily. Plain rod gaps always require the recloser to interrupt the short circuit caused when they flash over. With both types, service would be restored on the first reclosure, thus showing a higher percentage of operations without lockout than where valve-type arresters are used. However, most of these momentary circuit interruptions would be avoided with the valve-type arresters. It was suggested that another point of dissimilarity in records is the "yardstick" used for comparison of the improvement provided. Possibly, in lieu of a standard, comparison of actual operating results with the results of the mathematical studies might provide the needed uniformity.

The actual data and curves for manual sectionalizing presented by R. F. Quinn are a valuable addition to the studies presented in the paper. The curves in his Figures 1 and 2 should prove useful, as they can be plotted directly on the curves in the paper.

Modern Cathode-Ray Oscillograph for Testing Lightning Arresters

Discussion and authors' closure of paper 42-108 by E. J. Wade, T. J. Carpenter, and D. D. MacCarthy, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, August section, pages 549–53

O. Ackermann (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors of this paper have by necessity concentrated on explaining the details which make their equipment operate so successfully. This gives an opportunity to discuss the significance of their work in the field of cathode-ray oscillography in general.

As indicated in the bibliography to their paper, Dufour of France is the father of the high-voltage cold-cathode oscillograph. He used a mechanical sequence switch to apply high voltage to the cathode for a short time and to initiate the electrical phenomenon he wanted to study during this period. In this form, the instrument was introduced in the United States. The system soon proved too clumsy for the graphic recording of surges, to the study of which the instrument was applied. Harrington and Opsahl developed purely electrical high-speed switching by means of three-electrode gaps for the initiation of discharges in circuits serving cathode-ray oscillograph and surge generator. This resulted in a precision and rapidity of timing hitherto unknown. The circuits were published in the *Electric Journal* of August 1927 and have laid the foundation to a large number of systems of timing circuits interconnected by tripping spark gaps. The cathode-ray oscillograph with "impulse beam" was thus successfully established in the United States.

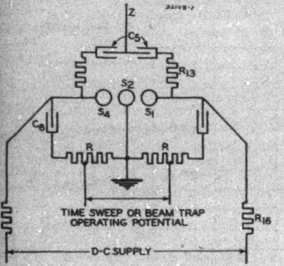


Figure 1. Completely balanced oscillograph trip circuit responding to impulses of either polarity at Z

Notations correspond to those of Figure 6 of the paper

In the meantime, many workers in Europe had concentrated on the design of oscillographs with continuously energized cathode and with systems of "beam traps" which kept the beam off the photographic film while it was not needed for recording. One such system, that of Doctor Norinder, was introduced in the United States by the Westinghouse Electric and Manufacturing Company in 1928 and was developed by the writer into a system of three or four cross-connected and balanced pairs of plates which, while "untrapping" or releasing the beam, do not produce a deflection of the latter in the recording part of the instrument. This has been disclosed in a paper of the writer in AIEE TRANSACTIONS, volume 49, 1930, page 467.[1] It should not have been necessary for the authors to suggest by their reference to a 1932 British paper that the method had originated outside of this country.

Interest was now focused almost exclusively on the oscillographs designed for continuous beam output. They attained considerable importance in the industry, and as is quite natural with an instrument finding wide application, they had to meet ever rising demands on their performance, particularly on writing speed. Increased writing speed is most directly obtained by raising the cathode voltage and the beam current which, in instruments designed for continuous output, meant complications such as artificial cooling of the discharge tube, higher insulation strength, and larger dimensions.

At this point, the system of the impulse beam, abandoned by most workers, but quietly fostered by the authors and their associates, bears new fruit, as it is, of course, entirely feasible to force a much higher discharge energy through a tube for a short time than the latter could handle continuously. This, however, is not the only thing which accounts for the extremely sharp and clean-cut pictures of 100,000,000-cycle transients obtained by the authors with their new "impulse-beam" oscillograph. In order to achieve this, the timing and beam release circuits must be very carefully laid out physically, shielded, and balanced. In this the authors have done a very good job, as every worker with oscillographs will acknowledge.

Regarding one feature of their circuit, namely, the four-electrode gap, it appears to the writer that a three-electrode gap should function in exactly the same manner. For the four-electrode gap of the authors to operate, *S*-3 must spark to *S*-4 first, and, once this spark is established, *S*-3 and *S*-4 act as one body, as they would if they were connected permanently.

We do not hesitate to give credit to the authors for the direct grounding of the center sphere *S*-2 but believe that the circuit could be simplified and modified as indicated in Figure 1 of this discussion, so as to respond even to surges of either polarity. A portion of the tripping impulse as divided by the resistors *R* and *R*-13 is impressed on spheres *S*-1 and *S*-4, adding to the potential to ground of one and subtracting from that of the other. Whichever potential exceeds the breakdown of the gap to *S*-2 will cause that particular half to flash over. Practically the full potential of *C*-5 now is applied across the other half of the gap which will then break down immediately. This, incidentally, also is the major function of *C*-5 in the authors' original circuit, although this is not mentioned in their paper.

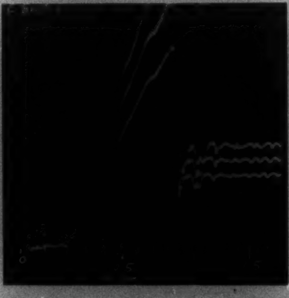


Figure 2. Typical high speed record obtained with continuously excited 50-kv oscillograph

A feature on which more information would be desirable is that of the cathode voltage regulator. One point in particular should have been mentioned in order to make the operation of the whole system clear, namely, that the left-hand terminal of the regulator must become grounded if a voltage of 80 kv is to be obtained at *G*-1.

That there are distinct possibilities in the method of impulse excitation is quite evident, as it has also been demonstrated by the work of Doctor Slack and his associates with impulse-excited high-voltage X Ray tubes. However, the continuously excited oscillograph is simpler in its operation and timing. It is, therefore, more desirable for general work. That it is entirely adequate for all practical purposes, which means to fractions of one microsecond, is demonstrated by hundreds of records such as that shown on Figure 2 of this discussion.

REFERENCE

1. A CATHODE-RAY OSCILLOGRAPH WITH NORINDER RELAY, ITS DESIGN AND APPLICATION. **O. Ackermann.** AIEE TRANSACTIONS, volume 49, 1930, pages 467–75.

J. M. Bryant and **M. Newman** (University of Minnesota, Minneapolis, Minn.): The authors are to be congratulated on making improvements in the cold-cathode cathode-ray oscillograph described in their paper. This type of oscillograph offers a better opportunity over the hot-cathode type for obtaining strong traces at ultrahigh frequencies and sweeps. A similar development is shown in technical paper 27, "Developments in High-Speed Oscillography," by J. M. Bryant and M. Newman, published March 1942 by the engineering experiment station of the University of Minnesota. In that paper new design features are given in sufficient detail to allow application to older slower-speed cathode-ray oscillographs. It is interesting that so many design features were solved in a similar manner and at about the same time but through independent work. The trace shown in Figure 8 of the paper under discussion compares in speed of sweep and writing speed with one published by ourselves in "Measurement of Very Short Time Lags," AIEE TRANSACTIONS, volume 59, 1940,[1] which we believe has usefully served as a goal to approach. Attention is called to the fact that "writing speed" is not necessarily limited by the velocity of light.

REFERENCE

1. MEASUREMENT OF VERY SHORT TIME LAGS, **J. M. Bryant, M. Newman.** AIEE TRANSACTIONS, volume 59, 1940, pages 812–16.

E. J. Wade, T. J. Carpenter, and **D. D. MacCarthy:** The authors are pleased to have received Mr. Ackermann's thorough discussion of our paper. We are familiar with the cathode-beam relay developed by Norinder and improved by Ackermann. We referred to the work of Burch and Whelpton since we preferred their type of beam trap, because it seemed simpler to build than the Norinder relay, and with it the beam is released by removal of the voltage from the plates. The plates of the Norinder relay must be perfectly balanced in order to avoid a deflection proportional to the voltage that is applied to release the cathode beam.

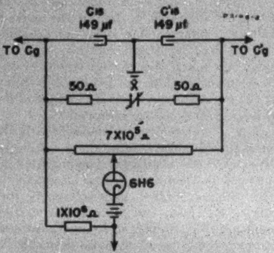

C16
149 µf
C'16
149 µf
TO Cg
TO C'g
50Ω
X
50Ω
7X10⁵Ω
6H6
1X10⁶Ω
TO GRID OF AMPLIFIER THAT CONTROLS
THYRATRON FOR RELAY OPERATION

Figure 3. Schematic circuit diagram for cathode voltage regulator

In Figure 1 of his discussion Mr. Ackermann gives a circuit for a three-electrode gap to replace the four-electrode gap shown in Figure 6 of our paper. In our opinion the four-electrode gap is advantageous, since it permits the sweep circuits to be started by an initiating impulse, lower in energy than would be necessary with the three-electrode gap. In order to initiate the complete breakdown of the three-electrode gap, the initiating impulse must change the voltage on one of the end gaps (see Figure 1 in Mr. Ackermann's discussion). This change in potential will cause current to flow through R_{16} to other portions of the high-voltage circuit and through the relatively low-impedance path through C_8 and R. The charges corresponding to these currents must be supplied from the cathode generator and tends to reduce the cathode voltage which should be constant. To start the operation of four-electrode gap, the initiating impulse has only to change the potential of the electrode S_2 which has a low capacity to other parts of the circuit. The time and charge necessary to do this is relatively small. Since the four-electrode gap is always initiated by an impulse from the cathode supply that is negative, there is no advantage in using a gap that will respond to surges of both polarities.

Mr. Ackermann has requested more information on the cathode voltage regulator. A schematic diagram of the regulator is shown in Figure 3 of this discussion. When the cathode generator is put on charge, relay X opens, and one of the capacitors, C_{15} and C_{15}', charges in series with each of the balanced banks C_g and C_g' of the Marx circuit shown in Figure 6 of the paper. The voltage across C_{15} and C_{15}' is proportional to the sum of the voltages on C_g and C_g'. The rectifier tube conducts when the drop on the potentiometer exceeds the bias voltage. This results in the operation of relays that initiate the sequence of operations described in the paper. When the capacitors C_g and C_g' are charged to 20 kv, the voltage on each of the capacitors C_{15} and C_{15}' is 67 volts. During the discharge of the cathode generator, the ground terminal of the capacitor C_g is 67 volts negative with respect to ground which is entirely negligible in comparison with the 80 kv supplied by the generator.

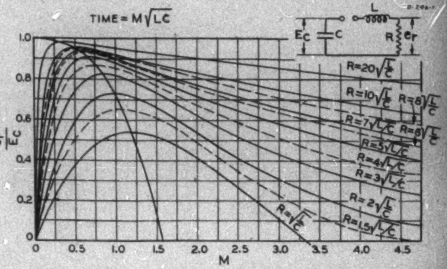
It is of interest to learn that Bryant and Newman have applied many of the same design features to a cold-cathode oscillograph that they have built.

Modern Impulse Generators for Testing Lightning Arresters

Discussion and author's closure of paper 42-96 by Theodore Brownlee, presented at the AIEE summer convention, Chicago, Ill., June 22-26, 1942, and published at AIEE TRANSACTIONS, 1942, August section, pages 539-44.

A. M. Opsahl (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): 1. In an impulse generator for lightning-arrester testing, the primary requirement is that it deliver an impulse test current of specified shape and magnitude through the test piece. The variation in voltage, electrostatic capacity, inductance, resistance, and discharge voltage characteristic of the test piece is so great as to make detailed calculations on each test circuit too laborious. For a number of years the curves in Figure 1 of this discussion have been found very helpful in determining the test circuit to deliver a specified test current.

Figure 1



2. As shown in the sketch, the capacitor discharges through the inductance L and load resistance R. The capacitor discharge formulas were used to obtain general curves applicable to any impulse current generator. The curves show the per cent of capacitor voltage that appears across load resistors. The load resistors are multiples of the impedance characteristic of the circuit $\sqrt{L/C}$. The time scale is plotted in multiples of the time characteristic of the circuit $\sqrt{LC}$.

3. The shape of the desired current wave determines the per cent of capacitor voltage available across the load resistor. The required current then determines the load-resistor magnitude. From this, a numerical value is obtained for the factor $\sqrt{L/C}$. From the required time to crest current in microseconds, a numerical value is obtained for the time factor $\sqrt{LC}$ in microhenries and microfarads. From a solution of these equations, the required capacity and inductance can be obtained.

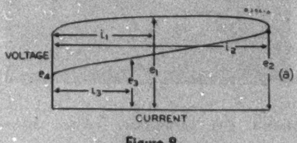


Figure 2

4. As stated by Mr. Brownlee, an arrester valve element is not a linear resistor, so the simple calculation above is not adequate unless the arrester is only a small part of the load resistor. As a matter of economy, to obtain the maximum test from the available equipment, the arrester is as large a part of the load resistance as possible. Mr. Brownlee's Figure 1B shows the arrester substituted for part of the load resistor. In Figure 2 of this discussion is a sketch of a typical volt-ampere curve of a valve arrester. On the rise in current, e_1 is substantially constant, and a better figure is obtained for the required circuit inductance by assuming the arrester is a constant voltage instead of a resistance element. The time to crest current does not vary rapidly with changes in circuit resistance. In the region from e_2 to e_4 there is substantially a straight line. For calculation of crest current, the arrester element can be regarded as a resistance $(e_2-e_4)/i_2$ plus a voltage e_4 which is subtracted from the capacitor voltage.

5. Equal impulse generator elements in series proportionately increase the voltage rating of the arrester element that can be tested, but do not change magnitude or shape of current wave. Equal impulse generator elements in parallel increase the available current without changing the current wave shape.

J. H. Hagenguth (General Electric Company, Pittsfield, Mass.): Mr. Brownlee has shown in his paper that the wave shape of the output currents of impulse generators can be calculated with very good accuracy, even though part of the discharge circuit contains an impedance which varies with the current. While experience certainly helps, a thorough understanding of the problem on the theoretical basis as shown in the paper certainly is required. For many years the tests on lightning arresters were further unnecessarily complicated by requirements of obtaining a definite voltage wave shape before arrester gap breakdown, followed by a definite current wave shape, so that impulse testing of arresters was really one of the most difficult of the impulse tests. It is therefore very gratifying that the author makes public the results of his experiences.

Of particular interest is the arrangement of generator 1 in a steel housing. Such a housing should go a long way toward eliminating many of the spurious oscillations which are liable to occur with the open type of generator. Here the stray capacitance formed between capacitors and ground in connection with the circuit inductance produce current flow through the ground circuit, thereby upsetting the balance between testpiece ground and oscillograph ground. The oscillograms presented show that this aim of confining stray capacitance currents has been completely accomplished.

The paper shows the growing importance of impulse tests in the design and research of lightning protective equipment. It is perhaps of interest to note that in the Pittsfield works of the General Electric Company alone, a total of 12 impulse generators is in more or less constant use for the determination of impulse properties of parts as well as of the completed products.

Tests on completed transformers are at times equally complex, not only on account of the complexity of the impulse discharge circuit because of the inductance to ground of the winding tested, but also because of the presence of the other windings on the same core. The voltages at the bushings of the other windings under conditions of test tend to rise up to and above the bushing sparkover, unless suitable impedance to ground is connected to them. While calculations can be made to analyze these voltages for a given transformer, the labor involved would be prohibitive. The electric transient analyzer is used for this purpose. Frequently such an impedance is obtained with Thyrite Since the transient analyzer operates at relatively low voltage (order of 1,000 volts), the amount of Thyrite, when checking with the analyzer, must be of necessity very much smaller than for the actual high-voltage test. It has been possible to match the Thyrite for the two tests effectively, so that for any voltage applied to the transformer, the wave shapes match perfectly. Such a match is of course important to obtain the necessary assurance that failure in the transformer winding did not occur. If the same amount of Thyrite is used for the 70 per cent check wave as for the 100 per cent full wave, the wave tail of the applied wave is liable to be considerably shorter on the 100 per cent wave. Thus, a failure in the winding producing a similar change in wave shape may be obscured.

Calculations such as shown in the paper are always desirable and necessary to obtain checks on the performance of equipment under impulse conditions imposing high stresses on the test specimen.

J. M. Bryant (University of Minnesota, Minneapolis, Minn.): The requirements for tests of all types of lightning arresters to meet standard specifications necessitate an impulse generator (or generators) providing a wide range of currents and voltages. We agree with Mr. Brownlee that under certain conditions a rapid way of determining the resulting current test waves would be from previously established curves for given conditions for a similar type arrester. As an example Figure 3 shows a group of current discharge waves through an arrester, all of which have practically the same specification. In addition to the impulse generator, it is necessary to have an accurate linear shunt for current, an accurate voltage divider for potentials, an oscillograph rapid enough to be capable of recording the events, such as described by Wade, Carpenter, and MacCarthy, and connections from the shunt and voltage divider which eliminate harmonics due to stray magnetic and electrostatic fields without the use of series resistances or other makeshifts which alter the actual values for the current or voltage curves. These specifications make it necessary to use infinite pains in making accurate tests. The author is to be congratulated in the clearness with which he has presented in so short a paper the results of what must have taken years of research and planning.

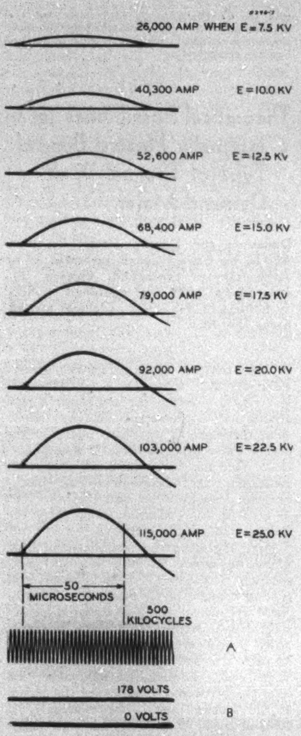


Figure 3. A group of current discharge waves through an arrester

A. Time scale calibrating frequency 500 kilocycles per second two microseconds per cycle

B. Current scale calibration. Shunt resistance 0.00278-ohm. Deflection equals 64,000 amperes

Theodore Brownlee: The discussions by Bryant, Hagenguth, and Opsahl agree well with the text of the paper. Mr. Opsahl's Figure 1 is equivalent to part of Figure 2 in the paper, and, over the range it covers, it permits wave calculations with about the same facility. His simplifications of the volt-ampere characteristic of a lightning arrester shown in Figure 2 are reasonable. Similar results will be obtained using his method or the curves in Figure 5 of the paper.

Regulated Rectifiers in Telephone Offices

Discussion and author's closure of paper 42-95 by D. E. Trucksess, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, August section, pages 613–17.

Clarence E. Lomax (Associated Electric Laboratories, Inc., Chicago, Ill.): This paper by Mr. Trucksess is very timely and is full of interest for all who have to do with the central-office power plant. It suggests to me a number of questions which I believe will be of interest to others.

1. Is supervision provided to indicate burnt-out tubes? If supervision is provided, is it equally effective on full load, light load, and no load?

2. Is there a means provided for extinguishing flashing when flashing occurs, and is it equally effective on full load, light load, and no load?

3. Does a charger which is rated at 30 amperes capacitance hold a constant voltage up to a 30-ampere load, or does the voltage start dropping before the full load is delivered?

4. When two chargers are required to operate in parallel, must any special action be taken to divide the load equally?

5. Is it necessary, when two chargers are used in parallel, to use chargers of equal capacity, equal age, and equal service usage?

Frederick L. Kahn (Automatic Electric Company, Chicago, Ill.): There have been a number of telephone-type regulated rectifiers on the same market which we are in the habit of referring to as "constant voltage rectifiers." These rectifiers use dry-plate rectifying elements (copper oxide or selenium) so that we have almost come to think of regulated chargers being synonymous with nontube-type chargers. We are very much interested to hear that equally close regulation of batteries in telephone exchanges can be obtained by 3-, 10-, and 30-ampere tube chargers, especially as the price of dry-plate units has made their use in chargers of higher capacitance almost prohibitive. The design of the tube-type regulated chargers at this time justifies the assumption that they have a lower first cost than constant voltage charging equipment of various types now available.

The following questions concern only the 48-volt rectifiers of 3-, 10-, and 30-ampere capacity:

1. As these chargers appear to operate continuously when used in regulated charging circuits, how often do tubes have to be replaced?

2. What is the electrical efficiency of these chargers? At what per cent of full load do they have maximum efficiency?

3. Are the 30-ampere chargers cheaper in first cost and operating cost than 25-ampere diverter pole motor generators?

4. What is lowest possible output of 10- and 30-ampere chargers when used in self-regulating service? (trickle charge rate)

5. What overload protection is provided if chargers are connected across a battery discharged during power failure?

6. How are variations of a-c voltage compensated for?

7. Do these rectifiers need any special adjustment for equalizing charge, or does the cutting in of additional countercells automatically raise the output voltage of the chargers?

D. E. Trucksess: The questions of C. E. Lomax and F. L. Kahn pertain to details in the design of the regulated rectifiers listed in Table I. These details and many more were not included in the original paper in order to keep it within the prescribed length.

In answer to Mr. Lomax:

1. In all cases some means are provided to indicate when a rectifier tube has failed. In most cases a voltage relay continually measures the regulated voltage, and, as soon as it goes out of the floating limits, it rings an alarm. In a few cases, a current transformer and relay are used with each individual bulb. The disadvantage of this type of alarm is that the alarm circuits cannot distinguish a tube failure from the condition of zero current produced by the regulating circuit as a result of no office load. Therefore, with regulated tube rectifiers the best alarm is a float voltage alarm.

2. In regard to flashover of the rectifier tubes, the thyratron tube, when used in 24-, 48-, or 130-volt rectifiers, does not flash over. The two-element-tube rectifier tubes flash over with voltage surges from the power line, and flashover relays are provided. These relays automatically disconnect the rectifier from the a-c line and battery during flashover and automatically reclose after the flashover is completed.

3. The 30-ampere regulated rectifiers hold a constant voltage up to full load, and at full load the circuit automatically transfers from a constant voltage-regulating circuit to a constant current-regulating circuit, holding the load at 30 amperes during any further charges in line or increase in load.

4, 5. When two or more regulated rectifiers are operated in parallel with regulation of better than two per cent, it is not practical for rectifiers to be operated under voltage control as the load will not divide equally, and it is impractical to attempt to automatically balance the load between them. When rectifiers, listed in Table I, are required to operate in parallel, means are provided for the constant voltage regulation to be transferred to constant current regulation. Therefore, rectifiers of unequal capacity, age, or service usage may be operated in parallel.

In answer to Mr. Kahn's questions:

1. In general, the tubes used in the regulated rectifiers shown in Table I will operate approximately one year at full load and from $1^1/_2$ to 2 years with the typical telephone type load of full load during the day and light load at night.

2. The electrical efficiency of regulated tube rectifiers is affected by the d-c battery voltage and the rectifier size; therefore, one figure will not cover the wide range of rectifiers listed in Table I. The maximum efficiency occurs at full load, which varies from 60 to 70 per cent for 50-volt rectifiers and 40 to 50 per cent for 24-volt rectifiers.

3. The 30 rectifiers listed in Table I have a cheaper first cost and operating cost than a corresponding size motor-generator set.

4. The 3-, 10-, and 30-ampere regulated tube rectifiers will operate and maintain their regulated voltage down to the trickle charge of the battery, which in most cases is one to five per cent of the full load rating of the rectifier.

5. Overload protection for regulated rectifiers is required in case the rectifier comes back after a power failure and attempts to float a discharged battery. In all cases the rectifiers shown in Table I have automatic overload protection. In the small rectifiers a ballast lamp causes the voltage to droop with overloads. In larger sizes the control is changed automatically from voltage to current regulation at full load.

6. The normal voltage regulation of the phase shift and booster types of control is sufficiently accurate to compensate for variations in the a-c line voltage. With the magnitude type of control, the normal regulations would not be sufficient, and it is supplemented by the line compensating circuit described as part of the combined rectifier and inverter.

7. In regard to equalizing, charging, or overcharging, the larger rectifiers have built into the control circuit a separate voltage control to increase the floating voltage from 2.15 to 2.3 volts per cell. With the smaller rectifiers, the regulating leads are connected to the load side of the countercells so that for overcharging the countercell switch is opened, and the rectifier automatically increases the battery voltage by an amount equal to the drop over the countercells.

Theoretical Possibilities in an Internally Heated Bimetal Type of Thermal Watt-Demand Meter

Discussion and author's closure of paper 42-110 by Edward Lynch, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, October section, pages 764–70.

P. M. Lincoln (Therm-Electric Meters Company, Inc., Ithaca, N. Y.): This paper is a very interesting one and shows a high ability for mathematical analysis. However, there are at least two points in the author's final conclusion that I would question. One has to do with his statement concerning the accuracy of the block-interval demand meter and the other has to do with his conclusion concerning the thermal demand meter.

First, regarding the block-interval type where, to quote his own words, "great accuracy is desirable." The block-interval type of demand meter measures the arithmetic average of the load under measurement. Any arithmetic average is necessarily a discontinuous function of time. As such, it is subject to a possible error of 50 per cent. To make matters worse, this error can be caused to appear at the option of the service user. The block-interval demand meter is perfectly accurate when we consider the particular time interval which it happens to select for measurement, but this time interval is only one out of an infinite number that might be selected. If Mr. Lynch will reread his reference 3, he will note that a series of tests made in Ithaca on block-interval demand meters extending over a period of 18 months showed a maximum error of 27.3 per cent and an average error of 10.1 per cent. In only ten per cent of the cases under measurement in Ithaca was there found no apparent error, and even in these cases there might have been errors if some intermediate time intervals had been chosen. Mr. Lynch's reference 3[1] also points out that maximum demand can be accurately measured only by a "continuous function of time." The logarithmic average is a continuous function of time while the arithmetic average is not and cannot be made a "continuous function of time." The block-interval demand meter is not only not accurate—Mr. Lynch to the contrary notwithstanding—but also cannot be made to be accurate.

Now, coming to Mr. Lynch's analysis of the thermal demand wattmeter, I question whether he carried his analysis far enough to arrive at an accurate result. The thermal wattmeter depends for its indication on the difference in temperature arrived at between two masses of matter which are heated by electric currents. This difference in temperature depends, in turn, not only in the difference in rate of heat application to the two masses of matter but also on the *rate of heat escape.* Final indication of the thermal demand meter is attained only when these two rates have become equal. If the rate of heat escape were always directly proportional to temperature elevation, Mr. Lynch's analysis would be perfectly correct and constitutes a beautiful piece of mathematical analysis. However, as pointed out in my paper, "An Improved Electrothermic Instrument,"[2] the rate of heat escape when such heat escape takes place by any method other than thermal conduction does not follow a first power law against temperature elevation. In the thermal wattmeter, where the actuating element is of bimetal strip as described by Mr. Lynch, most of the heat escapes by thermal convection. As proved by Chester W. Rice in his AIEE paper entitled "Free Convection of Heat in Gases and Liquids—II"[3], thermal convection varies as the 1.25 power of the temperature elevation—*not* the first power. Mr. Lynch does not take account of this in his analysis. I would, therefore, question the accuracy of the conclusions he arrives at in his paper.

I fully agree with Mr. Lynch that the "simple exponential is ideal." Due to the time taken for heat diffusion, the simple exponential cannot be entirely realized, but it can be closely approached. Why does not Mr. Lynch go still further and point out that the use of the thermal demand meter is the only known way in which maximum demand can be accurately measured?

REFERENCES

1. MEASUREMENT OF MAXIMUM DEMAND, P. M. Lincoln. AIEE TRANSACTIONS, volume 61, 1942, February section, pages 57–62.

2. AN IMPROVED ELECTROTHERMIC INSTRUMENT, Paul M. Lincoln. AIEE TRANSACTIONS, volume 54, 1935, May section, pages 474–81.

3. FREE CONVECTION OF HEAT IN GASES AND LIQUIDS—II, Chester W. Rice. AIEE TRANSACTIONS, volume 43, 1924, pages 131–43.

Edward Lynch: It was with much interest that I read P. M. Lincoln's remarks and studied his discussion. His first point can, perhaps, be more clearly understood without an opportunity for misunderstanding if we say, as he has, that "the block-interval demand meter is perfectly accurate" in indicating continual arithmetic average demand and add, as I have, that the thermal demand meter indicates a slightly different quantity which has been designated as continuous logarithmic average demand. The accuracy and precision of both types of meters as measuring devices is of such a high order that each can be built to measure the quantity for which it is intended and with accuracies well beyond requirements of normal use.

The second point in question illustrates an important "design principle." Heat escape from the actuating elements of thermal meters should be by controlled conduction rather than by radiation or convection. In the mathematical analysis this means that heat escape is proportional to the first power of temperature rise.

Doctor Lincoln stated that in the thermal wattmeter where the actuating element is a bimetal strip most of the heat escape is by thermal convection. This was not the case in the internally heated bimetal meter designed to check the analysis made in the main paper. In fact, the internally heated bimetal meter inherently fosters heat escape by conduction. This is easily realized when one recognizes the close correlation between electrical and thermal conductivity. Since we must have good electrical conductivity into and out of and between the actuating elements we readily have available good thermal conductivity as well. This was taken advantage of in the design of the meter on which tests were made, and the results of these tests substantiated the analytical conclusions reached.

A New Jewel for Indicating Instruments

Discussion and authors' closure of paper 42-134 by F. K. McCune and J. H. Goss, presented at the AIEE summer convention, Chicago, Ill., June 22-26, 1942, and published in AIEE TRANSACTIONS, 1942, September section, pages 673-6.

John H. Miller (Weston Electrical Instrument Corporation, Newark, N. J.): The description of the test methods used on the new glass jewel for indicating instruments by McCune and Goss and the tabulation of the results are currently of importance to those using electric indicating instruments, because it indicates the dynamic character of the art and is assurance that functional quality is being maintained in such instruments, even in spite of important material difficulties.

The Weston Electrical Instrument Corporation is manufacturing glass instrument jewels in large quantities, and these jewels are being used with eminently satisfactory results in many fields. Aircraft instruments, incorporating electric mechanisms, are being almost wholly supplied with this type of bearing.

About ten years ago the Weston Electrical Instrument Corporation started an investigation of substitutes for sapphire jewels to obtain a more perfect contour and surface than was possible in sapphire, as was required for highly sensitive instruments. Glass jewels were known and used at that time; in fact, a patent was issued as far back as 1899, covering glass *V* jewels, but none was found of satisfactory quality for use in Weston instruments.

Preliminary work was therefore done on the development of a satisfactory glass jewel in the Weston laboratories, and by 1934 and 1935 extensive field tests on a new form of glass jewel showed it to be equal to and in many respects superior to sapphire, particularly where vibration was encountered.

In view of the evident usefulness of this type of bearing, as well as a matter of protection against the possibility of cessation of sapphire-bearing imports, a small pilot plant was installed about 1938 to establish a satisfactory production technique. When the war broke out in Europe, and shipments of sapphire bearings were cut off, a very large number of these glass jewels were supplied to Sangamo-Weston, Ltd., England, whose tests confirmed their usefulness in aircraft instruments. It is understood that today practically all electrical instruments used by the Royal Air Force, which were manufactured in England, carry jewels of this type, now manufactured in that country.

This extensive manufacturing experience enabled the Weston Company to expand the pilot plant to a full-fledged production basis as sapphire bearings became more difficult to obtain in the United States and that expansion has been continuous and is still going on.

The Weston glass jewel, known as the Weston *Y* bearing, has been developed with perhaps greater stress on the matter of surface finish and resistance to vibration than to the factor of shock, although shock tests of the order of 250*G* appear to be required in any direction before pitting takes place with a moving system having less than one gram weight. As is indicated by McCune and Goss, vibration tests have been surprisingly satisfactory, and it seems apparent from such tests conducted in the Weston laboratories, at the Bureau of Standards, and elsewhere, that actual jewel wear is materially less under conditions of severe vibration with the *Y* bearing as compared to the sapphire. The perfection of the surface finish is probably responsible, as well as the fact that the bearing more nearly approaches the hardness of the pivot, thus reducing the tendency of the pivot to wear.

While it is admittedly difficult to cut a perfect pit in a sapphire, it is hardly believed that variation in lineal cutting speed from center to periphery, as referred to by McCune and Goss, is responsible for the difficulty, since the cutting tool traverses any point in the sapphire once every revolution, and will take off as great a thickness of material near the center as near the edge. Wear on the cutting tool is perhaps a function of cutting speed, but it is believed that cutting speed is not a factor with regard to the relative depth of material removed per revolution.

It is interesting to note that the coefficient of friction varies so little between materials, and it seems evident that the criterion in the bearing is not so much the friction coefficient, but rather its ability to maintain its original shape and surface under conditions of severe vibration, shock, and use. It seems quite probable that these new special glass bearings, as developed in the several laboratories, will find fields of usefulness in future years in small instruments with light elements, where high sensitivity is required, even though sapphire bearings again become as plentiful as in years gone by.

F. K. McCune and J. H. Goss: Mr. Miller has presented an interesting history of the Weston Electric Instrument Corporation's experience with glass jewels. His findings are in general agreement with ours and are especially interesting on vibration. Work not complete at the time of writing the paper has shown the following curve to be typical of instrument performance after severe vibration.

Mr. Miller has stated "shock tests of the order of 250*G* appear to be required in any direction before pitting takes place" for a moving system of one gram weight.

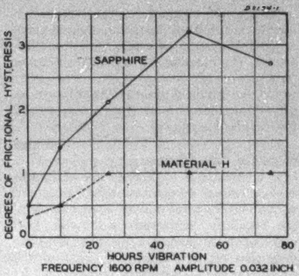


Figure 1

Our experience has indicated that such a value may depend materially on the speed with which the acceleration reaches its maximum and varies with the rigidity of the moving system in question. Work done on a device in which the motion was of the form of a damped sine wave with a time of 1.5×10^{-3} seconds from initiation to completion of a half-cycle gave very different values from those obtained with a slow rate of loading the jewel surface. However, the value at which the pivot was mushroomed in the case of the sapphire was approximately that at which a blemish would be produced in the jewels of material *H*.

In conclusion, the authors are both continuing work on the *V* jewels and progressing the development of ring jewels from the same material. Many hundreds of thousands of the *V* jewels have already been produced with characteristics as represented in this paper.

A New Moving-Magnet Instrument for Direct Current

Discussion and authors' closure of paper 42-123 by H. T. Faus and J. R. MacIntyre, presented at the AIEE summer convention, Chicago, Ill., June 22-26, 1942, and published in AIEE TRANSACTIONS, 1942, August section, pages 586-8.

John H. Miller (Weston Electrical Instrument Corporation, Newark, N. J.): A comparison of the energy for full-scale deflection taken by the new moving magnet instrument mechanism of Faus and MacIntyre, with that required for conventional mechanisms may be of interest.

It is stated in the paper that with a moving element weight of 0.205 gram and an effective torque for a full scale of 0.35 millimeter-gram, giving a torque-to-weight ratio of 1.7, the coil energy was 0.05 watt for full scale.

Conventional small a-c iron-vane instruments, with somewhat heavier moving elements, on the basis of springs to give the same torque-to-weight ratio, would require 0.5 watt. In other words, the new instrument discussed has an energy sensitivity something like ten times that of conventional soft-iron instruments, which have been used for many years.

Making the comparison with permanent-magnet moving-coil instruments, and using a moving system of practically the same weight, we find that for one of the simpler structures with moderate damping, there would be required about 0.00036 watt, or something like $^1/_{150}$ of the energy needed for the new instrument.

However, this is on the basis of a design of nearly three decades ago, and more recent instruments compared on the same basis of equal torque-to-weight ratio, and with about the same moving element weight, are regularly being produced with high coercive force magnets to give full-scale deflection on 0.000025 watt, $^1/_{2,000}$ of the energy taken by the new instrument.

It is believed that this comparison with equal torque-to-weight ratios is quite valid, since, obviously, both old and new types of mechanisms can be supplied with bearings of any one of the several types currently used, and, with similar bearings and similar moving system weights similar torque-to-weight ratio should be the determining criteria.

While it is admitted that iron-vane instruments of conventional design have a considerable d-c error, this, of course, can be reduced, if important, by the use of suitable material in the iron vanes.

In the relatively wide energy span separating the conventional iron-vane instruments and the best of the permanent-magnet moving-coil instruments, a span from 500,000 down to 25 microwatts, or a factor of 20,000 to 1, this new design, on a comparable torque-to-weight ratio basis, has only progressed one decimal place from the conventional iron-vane type.

Since the only advantage of the new mechanism is low cost, one wonders if it is sufficiently less costly to justify the relatively high energy taken from a circuit which effectively limits its broad applications.

J. R. Macintyre and **H. T. Faus:** A comparison of the moving magnet instrument with the iron-vane a-c instrument is not relevant, since reasonably accurate and well-damped instruments of this type are as expensive as moving coil instruments. Our comparison was with the lower-cost polarized vane instrument which has the additional disadvantages of poor scale distribution, short scale, and poor damping.

The comparison of the microwatt sensitivity of the moving magnet instrument with that of the moving-coil instrument does not give an accurate picture of the relative merits of the two designs. For example, Mr. Miller gives a figure of 0.000025 watt for full-scale deflection on a sensitive moving-coil instrument. Using this figure, one not familiar with instruments might assume that a ten-ampere ammeter could be built with a resistance of $\frac{0.000025}{100}=0.00000025$ ohm. This is not the case, because springs of the required low torque cannot be built to carry ten amperes, much less to have a resistance as low as the above value. The general practice in a moving-coil instrument of this rating is to use a shunt with a 50-millivolt drop and to connect the instrument winding, in series with a suitable resistor, across this shunt. The net result is an instrument with an over-all loss of 0.5 watt. An instrument of the same rating in the moving magnet design would have a winding consisting of a single turn of heavy copper wire occupying the entire winding space. The total watts loss of the instrument would be about 0.05 watt. The addition of a shunt to give ambient temperature compensation equivalent to that of the moving-coil instrument would not increase this to more than 0.1 watt, or one-fifth the power required by the complete moving-coil instrument.

The general practice is to build voltmeters with a full-scale current of ten milliamperes except for particular applications where low current consumption is required. Using a full-scale current of ten milliamperes, a moving magnet voltmeter with jeweled bearings can be built in full-scale ranges from ten volts up with a torque-to-weight ratio adequate to insure a sustained accuracy of two per cent of full-scale reading. This is equivalent to that usually guaranteed for minature moving-coil instruments. It refers to the exact construction shown in the paper and by no means represents the ultimate sensitivity obtainable in moving magnet instruments. Since the over-all power consumed by both types of instruments is the same, the moving magnet design is at no disadvantage because of the greater power consumed by its element.

From the foregoing it is apparent that there is a wide range of instrument ratings in which no use can be made of the maximum sensitivity of the moving-coil instrument and in which the moving magnet instrument is at no disadvantage due to its lower sensitivity.

The authors felt that the implied advantages of the instrument would be apparent to those familiar with instruments. However, in view of Mr. Miller's comments, "Since the only advantage of the new mechanism is low cost," it may be well to bring some of these advantages into greater prominence.

The maximum change in reading of the moving magnet instrument in a ten-gauss field is one half of one per cent of full-scale reading. The corresponding change for a moving-coil instrument is of the order of three per cent. The stray field produced by the permanent magnet of a moving-coil instrument will cause a considerable error in the reading of another moving-coil instrument when mounted close to it and is sufficient to cause an appreciable error in a magnetic compass at a distance of several feet. Mounting on a steel panel will change its calibration because of the shunting effect of the panel on the instrument magnet. Effective shielding to overcome these deficiencies is difficult, because a shield close to the magnet will shunt a large part of its flux, and one far from it will increase the outside dimension of the instrument. In either case the calibration difficulties will be increased, as the instrument must be calibrated with the shield in place, and the shield interferes with the stabilizing a-c knockdown treatment which should be given to the instrument magnet. None of these difficulties are experienced with the moving magnet instrument. These instrument elements may be mounted as closely together as their physical dimensions permit. Since their dimensions are so much smaller than those of moving-coil instruments, it is possible to mount more of these instruments in a given space. This is a very valuable feature in many applications.

Another valuable feature of the moving-magnet construction is the readiness with which it may be adapted for measuring current ratios by substitution of another fixed coil for the control magnets. The instrument then becomes a ratio meter and the scale distribution may be predicted by the same vectorial construction shown in Figure 4.

Emergency Overloads for Oil-Insulated Transformers

Discussion and authors' closure of paper 42-101 by F. J. Vogel and T. K. Sloat, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, September section, pages 669–73.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): This paper adds some further data on the aging of insulation and gives some proposals on how to use the data. I cannot agree with some of the claims set forth in the paper, for example, the statement that at 120 degrees centigrade the tensile strength drops to approximately 50 per cent and levels off. Further, it states that it would appear that this 50 per cent strength would be maintained for long periods of time. This is an important point, because, if true, it would permit long-time operation at 120 degrees centigrade without any further sacrifice in the life of a transformer. As a matter of fact, the data in Figure 5 on Fuller board aged in oil protected by nitrogen show a continued decrease in tensile strength.

I agree that the way in which the curves are plotted in Figure 1 might lead one to assume that the tensile strength had leveled off. All aging data that I have ever obtained fell on a straight line when plotted on semilogarithmic paper, with time on the logarithmic scale and temperature on the uniform scale and temperature on the uniform scale. In fact, all of the aging data shown in Mr. Clark's paper fall on a straight line indicating in some cases and actually showing in other cases that the tensile strength continues to deteriorate at the same semilogarithmic rate until zero life is reached. This is one nature's laws discovered many years ago by Sir Isaac Newton and known as the "die away curve."

I have taken the liberty of replotting the data shown in Figure 1 of the authors' paper on semilogarithmic paper. Figure 1 indicates that the tensile strength does not level off but apparently continues to deteriorate at the same rate. When replotted on semilogarithmic paper, and extrapolation of the data shown in their Figures 5 and 11 indicates the 135 degrees centigrade life is longer than the 120 degrees centigrade life. Furthermore, there appears to be no consistent rule in degrees increase to double the rate of aging as shown by these curves.

In a discussion of Figure 5 it is stated that the markedly lower strengths obtained with pressboard and varnished cambric, as compared to Manila paper alone, are due to higher acidity. However, these tests were made with oxygen-free oil, protected by a nitrogen atmosphere. These results are therefore inconsistent with the claim that the panacea is to provide an inert gas cushion above the oil.

The assumption that the hot-spot rise over top oil varies as the loss 0.8 can be used for all classes of transformers, particularly small distribution-type coils, does not agree with our findings. For instance, we have found that under the condition where the 0.8 power holds for horizontal coils, for vertical coils the exponential power ranges from approximately 0.9 to 1.0, depending on the thickness of the winding. For thick windings having high internal gradients and small surface drop, the power is close to unity as would be expected. In general, for thin single-layer vertical coils, the power is approximately 0.1 higher than for horizontal coils. This is, of course, due to the greater effect of the viscosity of the oil for horizontal than for vertical coils at the lower temperatures, whereas for the higher temperatures the difference in the effect of viscosity tends to disappear. This effect on the temperature rise of the horizontal and vertical coils is very well illustrated in Figures 3 and 4 of reference 1.

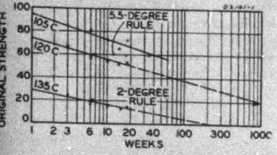


Figure 1. Tensile strength of Manila paper in oxygen-free oil protected by a nitrogen atmosphere from Figure 1 of paper by Vogel and Sloat

REFERENCE

1. HOT-SPOT WINDING TEMPERATURES IN SELF-COOLED OIL-INSULATED TRANSFORMERS, F. J. Vogel, Paul Narbutovskih. AIEE TRANSACTIONS, volume 61, 1942, March section, pages 133–6.

Jerome J. Taylor (Detroit Edison Company, Detroit, Mich.): The paper is very fine in showing effects of thermochemical factors on the mechanical strength of transformer insulation. This is a valuable but somewhat more specialized study than indicated by its title. From an operating standpoint, a correlation between mechanical strength and electrical failure still has to be made; this subject is barely touched. Destructive testing of small motor stators has been recently reported.[1] It appears that similar tests of (small) transformers are practical and that the results would add interpretative value to the more difficult studies already accomplished.

REFERENCE

1. TEMPERATURE-AGING TESTS ON CLASS-A-INSULATED FRACTIONAL-HORSEPOWER MOTOR STATORS, J. A. Scott and B. H. Thompson. AIEE TRANSACTIONS, volume 61, 1942, July section, pages 499–501.

F. Von Voigtlander (The Commonwealth and Southern Corporation, Jackson, Mich.): The authors have shown that transformers of the usual design can probably be considerably overloaded without serious effect on their life, and there appears to be unanimity of opinion on this point throughout the industry. The writer would like to call attention to the fact that overloading transformers also proportionally increases the transformer regulation. Furthermore, much of the load growth which is requiring the overloading of transformers is industrial load, usually of lower power factor than the system average. This may result in a considerable increase in the system requirements of reactive power, which would also increase the regulation of the transformers. Superimposed on this is the increase of reactive losses in the transmission system caused by the increased line loadings at lower power factors. If sufficient reactive capacity is not available near the load centers, the system voltages may sag considerably, thereby further adding to the reactive power demands.

It therefore follows that, unless the system is unusually well supplied with reactive capacitance, considerable attention should be paid to this matter of adequate voltage, as otherwise it is possible that the overload ability of transformers cannot be fully realized because of distribution voltage limitations, and no substitute for volts at the customer's service has yet been found.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): The results shown in Figure 1 of the paper are very surprising in that they indicate practically no deterioration in the aging tests between 6 and 18 weeks. According to these data, insulation kept indefinitely at 105 degrees centigrade would remain at 70 per cent of its original strength, and 120 degrees could not produce more than 50 per cent deterioration at any time. In the oven-aging tests in Chicago on sealed oxygen-free samples of various makes of impregnated-paper insulated cables, which were described in my paper on "Load Rating of Cable—II,"[1] a sharp decrease in tearing strength occurred in tests at 125 degrees centigrade between two and five months. This decrease was found for each of the several samples tested. The studies made years ago at the Massachusetts Institute of Technology, on the effect of heat on paper insulation, corroborate the continuously decreasing trend of mechanical strength with time of aging, found in the recent Chicago tests. Possibly, tests on a large number of samples for durations beyond 18 weeks would modify the slope of the curves in Figure 1 of the paper.

Table I

Test Temperature Degrees Centigrade	Mechanical Strength After 100 Days—Per Cent of Original Value		
	Chicago Tests		Tests by Vogel and Sloat Figure 3
	Average	Minimum	
100	99	91	78
125	67	54	32

Also, the slopes of the curves in Figure 3 of the paper, which are obtained by cross-plotting from Figure 1, are subject to question. The 20, 40, and 50 per cent curves are based on only one measured point, and the 60, 65, and 70 per cent curves are based on two points. The uncertainty of the slope of the curves is illustrated by the fact that, according to Figure 1, the strength remains at 54 per cent in the 120-degree test between 8 and 18 weeks while, according to Figure 3, the strength decreases at 120 degrees from 54 per cent after eight weeks to 49 per cent after 14 weeks.

Table I of this discussion is a comparison of the results obtained in the oven-aging tests in Chicago, Figure 1 of my paper on "Load Rating of Cable—II," with values taken from Figure 3 of the paper by Vogel and Sloat.

Do the authors have an explanation for these large discrepancies?

REFERENCE

1. LOAD RATING OF CABLE—II, Herman Halperin. AIEE TRANSACTIONS, volume 61, pages 930–42.

F. M. Clark (General Electric Company, Pittsfield, Mass.): My interest in this paper concerns primarily the following statement which is made in the general summary:

> "Acids are formed in service by contact between oxygen and transformer oil. Excluding this contact by blanketing the transformer with inert gas results in a high mechanical strength of the insulation at a given temperature and time or conversely permits a higher operating temperature."

On the basis of the data submitted in the paper this statement can be true only within definite limits. It cannot be true at 135 degrees centigrade, for, if one compares the data for Manila paper of Figure 1 and Figure 11, one finds no real advantage from the use of the nitrogen gas. I have reproduced in Figure 2 of this discussion the pertinent data presented by the authors. A relation such as this indicates that at 135 degrees centigrade heat alone causes paper deterioration at a rate which is not dependent on the oxidation effects referred to by the authors.

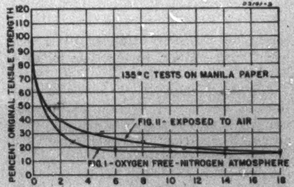


Figure 2

Although the authors do not illustrate the behavior of oil-treated Fuller board aged in the presence of oxygen the remarkable stability of the Fuller board in the absence of oxygen (Figure 9 of the paper) might possibly be taken as support for the author's contention that oxidized oil is responsible for the mechanical deterioration of the cellulose. The Fuller board of Figure 9 is characterized by substantially no decrease in mechanical strength during a period of 18 weeks at 120 degrees centigrade. This apparent mechanical stability is contrary to the data accumulated in our own laboratory and does not agree with the authors' data of Figure 1 for Manila paper, where a loss of 50 per cent in tensile strength is shown for the same oxygen-free test conditions. Neither do the data agree with the previously published work of C. F. Hill to which the authors refer. Mr. Hill states that cellulose is "subject to both temperature and oxidation effects, the temperature effect of course taking place above 105 degrees centigrade."

In Figure 3 of this discussion I have compared the data on Fuller board presented by Mr. Hill with that presented by the authors, converting Mr. Hill's data to percentage of original tensile strength in order to make the comparison clear. Although both investigators are in fair agreement at 135–140 degrees centigrade aging, there is a marked divergence in the data presented for the temperature range from 105–120 degrees centigrade. In our own laboratory we obtain, as does Mr. Hill, a decrease in strength as the aging progresses, but, unlike Mr. Hill, we find no stabilization in mechanical strength after about 50 per cent of the initial strength has been lost. The mechanical deterioration in the studies which we have carried out proceeds continuously in a semilogarithmic relation to the time factor of heating.

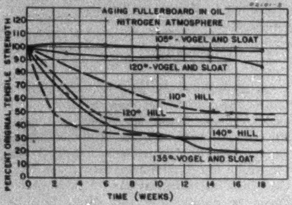


Figure 3

Our own explanation of the fundamental cause of mechanical deterioration in cellulose differs greatly from that offered by the authors who attribute it to acid formation in the oil. I am not especially concerned with the data presented which test the combined effect of Fuller board and varnished cloth. Obviously acids derived from sources foreign to the cellulose or the oil may or may not affect the mechanical strength of the cellulose, depending upon their chemical natures Their presence, as described by the author. in Figure 6, is not dependent on the presence of oxygen. No data are presented by the authors correlating the behavior of such combinations of materials with the thesis advanced. Furthermore, the effect of acids derived from varnished films appears to be of but academic interest, for, as the authors state, acid formation from this source has been practically eliminated by the use of synthetic materials. I am, however, concerned with the suggestion that acid formation in the oil through oxidation is responsible for the mechanical deterioration. I call attention to the fact that the 50 per cent deterioration in the mechanical strength of Manila paper in Figure 1 was obtained with substantially no change in the oil acidity.

Elimination of acid formation as the result of oxidation in mineral oil can be brought about by a variety of methods. Only one of these methods, the elimination of oxygen, produces any substantial effect on the mechanical aging of cellulose. I suggest that the presence of oxygen affects the mechanical life of oil-immersed cellulose because of its direct oxidation action on the cellulose. As such, as suggested by Mr. Hill the effect of oxygen is most important for the lower temperatures applied. As the temperature is raised, the mechanical deterioration because of heat alone becomes of increasing importance. This type of deterioration involves the formation of acids derived from the cellulose and occurs whether or not oxygen is present. The use of nitrogen to eliminate oil acidity is not of fundamental importance. Although the elimination of oxygen may be of academic value in promoting the mechanical stability of cellulose insulation if a transformer is operated at temperatures of 75–95 degrees centigrade, it must be remembered that the oxidation reaction itself proceeds so slowly at these temperatures that it is demonstrated only with great difficulty. Higher temperatures of operation in the range of 100–120 degrees centigrade, or higher in the case of overload conditions, bring into play the thermal type of cellulose deterioration. This type of deterioration becomes of greater importance as the temperature and the duration of its application are increased. In a general sense, we have found that the insulation will age at a rate not materially affected by the presence or absence of mineral oil. Acids derived from mineral transformer oil of the American type have not been found to be a dominating factor in determining the mechanical life of the insulation.

T. K. Sloat and F. J. Vogel: We desire to acknowledge Mr. Von Voigtlander's discussion, and it is true that regulation must be considered in any application.

With reference to Mr. Taylor's discussion, tests on small transformers have been made and indicate that they are still operative, even after thousands of overloads and high temperature cycles. The results of these tests and others will undoubtedly furnish the material for papers which will be presented at a later time.

We are rather surprised at the small amount of deterioration shown in Mr. Halperin's discussion. Neither Mr. Clark's nor our tests indicate such high values after 100 days at 100 or 125 degrees centigrade. Mr. Halperin has criticized the construction of Figure 3. For purposes of analysis, data in the form of Figure 3 were more valuable than Figure 1, which was the original data. When curves are drawn with different parameters, unless the data are extremely accurate, some variations are to be expected. In this particular case, the very nature of the materials led to variations which could not be eliminated except by an extremely large number of tests and samples.

Mr. Montsinger, in his discussion, clearly points out his difference of opinion as to the characteristics of insulation deterioration. He is not, however, entirely consistent in his criticisms. He refers to Figure 5 as indicating that insulation shows a continuous decrease in tensile strength, even when protected by nitrogen. In that case, the introduction of varnish-treated material is shown to be the cause, and even an inert atmosphere will not provide protection when such materials are used. Mr. Clark, in his discussion, clearly took this point into consideration.

Mr. Montsinger has seen fit to replot the data of Figure 1 on semilogarithmic paper. He, however, first assumes that the data should be represented by straight lines on semilogarithmic paper. If that assumption were not made, curves could equally well be shown as flattening out in the period between six to ten weeks and results nearly constant after 10 to 15 weeks.

In considering Mr. Clark's discussion, one might think that we recommended temperatures in the neighborhood of 135 degrees centigrade for operation. This is not true. Within definite limits, excluding oxygen does result in a higher mechanical strength of insulation for a given time and temperature. It is generally well recognized that thermal decomposition of cellulose takes place at a temperature above 125 degrees centigrade, and the advantage of an inert atmosphere would be partially lost if operative temperatures in this range were continuously employed.

Table II

Temperature Limits	Oxidation	Thermal Decomposition
Clark	75–100	Above 100
Sloat-Vogel	75–120	Above 120

With respect to the discussion regarding the effectiveness of acids, additional tests made at the same time as those represented show that Fuller board is affected by acids. It might be pointed out in all cases the acids referred to contain some peroxides. The peroxides are the detrimental agents, as they cause cellulose deterioration. However, it is quite well accepted that the acids are a measurement of the peroxides present.

With respect to Mr. Clark's discussion regarding the mechanical stability of cellulose insulation as affected by oxygen, one might be led to believe that the rate of insulation deterioration at the lower temperatures is so slight that it would be demonstrated with great difficulty, and that there is less and less difference as the temperatures become higher. One of the best evidences to the contrary is the comparison between Figure 1 and Figure 11 of the paper. In Figure 1 at 105 degrees centigrade, the insulation strength seems to be about 70 per cent of its original value after eighteen weeks at 105 degrees centigrade, whereas, with air present it was down to 35 per cent and decreasing rapidly. There is, however, some agreement with Mr. Clark, after all, in the results of our tests. We agree on the types of cellulose deterioration, but not in the degrees of temperature at which the types of deterioration take place as shown in Table II of this discussion.

It should be noted, that there is no sharp line of demarcation. Both effects overlap to some extent. Temperature limits in Table II should be considered as those limits within which either oxidation or thermal decomposition are predominate factors.

Factors Affecting the Mechanical Deterioration of Cellulose Insulation

Discussion and author's closure of paper 42-98 by F. M. Clark, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, October section, pages 742–9.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): F. M. Clark's paper adds a definite milestone in determining the effect of temperature and moisture on the aging of insulation. For several years we have been in need of more exact data on how the higher temperatures affect the rate of aging of cellulose insulation. While I have used the eight-degree rule in deriving short-time overloads where the temperatures were considerably in excess of 110 to 120 degrees centigrade, I had no definite proof that it held for the higher temperatures. Consequently, I am considerably relieved to learn that the eight-degree rule holds up to 200 degrees centigrade.

Of course, it should not be overlooked that Mr. Clark finds that for temperatures in the order of 90 degrees centigrade to 105 degrees centigrade, a rule less than eight degrees holds (in the order of three to six degrees), and for lower temperatures a still lower rule, as low as two degrees, holds.

Based on Mr. Clark's data, the eight-degree rule should be used in estimating short-time overloads where the hottest spot temperature is in excess of approximately 110 degrees to 115 degrees centigrade, and approximately a five-degree rule when estimating the effect of temperatures in the neighborhood of 90 degrees to 100 degrees centigrade on the life of transformers. There is one very important way in which the five-degree rule can be used, namely, to see whether it is economical to increase the temperature rise of transformers above 55 degrees centigrade. For example: by the five-degree rule, the life of a 60 degrees centigrade rise transformer is half that of a 55 degrees centigrade rise transformer. The extra capacity gained when increasing the average rise from 55 degrees centigrade to 60 degrees centigrade, is in the order of six to seven per cent, and the gain in cost is in the order of three to four per cent. Consequently, by this process of reasoning, we save an initial investment of three to four per cent for a transformer having one-half the life. The initial saving in cost does not sound like a good economical proposition.

There is one very important point that has been settled by these tests, namely, that the effects of several overloads on the life of a transformer, are additive. This has generally been assumed in the past, but we had no definite proof that these assumptions were correct.

Mr. Clark's discovery of the important role that water content plays in the rate of aging also explains why my early tests showed that the insulation aged faster in mineral oil than in air—oven aged. Since the importance of keeping the insulations perfectly dry was not realized, the samples no doubt picked up considerable water between the drying period and the starting of the aging tests. The moisture that was picked up previous to the oven- or air-aged samples naturally was driven off immediately, while the oil-immersed samples retained their moisture to a certain extent.

To quote from my 1930 AIEE paper on "Loading Transformers by Temperature":

> "No completely satisfactory explanation has ever been given for this shorter life of insulation (aged) in oil than in air, though it has been suggested that it may be due either to the oil softening the fibers, or the acids in the oil attacking the fibers. This is a good subject for a physicist to study."

While this paper does not show comparative aging in air and in oil, such tests have been made under closely controlled moisture conditions, and no material difference was found in the rate of aging. Apparently this question has been cleared up, and we can no longer claim that the life of cellulose is longer in air than in properly protected oil, provided, of course, that the insulation is dry.

REFERENCE

1. LOADING TRANSFORMERS BY TEMPERATURE, V. M. **Montsinger.** AIEE TRANSACTIONS, volume 49, 1930, pages 776–92.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): I have followed with interest Mr. Clark's series of papers, which always contain a large amount of useful information. Figure 6 is of special interest, but it seems that some caution should be used in applying these data. The figure indicates that toward the end of the life in the range of 75 to 100 degrees centigrade, an increase in temperature of 2 or 2.4 degrees cuts the remaining life in half, and the conclusion may be drawn that a slight temperature increase is a serious matter. It should be noted, however, that this conclusion is based on values extrapolated from about 100 weeks to 2,000,000 or 5,000,000 weeks and from about 25 per cent reduction in strength to 100 per cent reduction in strength. It is quite possible that actually the slope of the 75-degree curves in Figures 3 and 4 might be quite different for long durations or, in other words, that the total life is quite different from the extrapolated values of 2,000,000 or 5,000,000 weeks. Such a change would, of course, affect the relation between rate of deterioration and temperature increase.

From a practical standpoint the upper portion of Figure 6 is relatively most important, that is, down to 50 per cent of the initial strength. The average for the first 50 per cent of the mechanical life consumption of the temperature increase to double the rate of deterioration ranges from about six to ten degrees centigrade which, for practical purposes, agrees quite well with Montsinger's eight-degree rule.

Figure 4 of Mr. Clark's paper shows much greater deterioration than indicated in oven-aging tests in Chicago of sealed cable samples, which are described in my AIEE paper on "Load Rating of Cable—II."[1] As in both Mr. Clark's and the Chicago tests, the insulation was vacuum dried, and oxygen was excluded, the tests should be expected to be comparable. Figure 4 of Mr. Clark's paper shows for one year at 100 degrees centigrade a reduction in strength of about 35 per cent while the same test period in Chicago produced for a large number of samples an average reduction in strength of four per cent and a maximum reduction of 19 per cent. To obtain a 50 per cent reduction in mechanical strength in one year, a temperature of about 102 degrees is required, according to Mr. Clark's tests, while the tests on short cable samples and on large trial field cable installations in Chicago indicate that about 120 degrees is required. Possibly, the difference in cable oils and transformer oils had some effect on the large resultant difference of 18 degrees centigrade, but that explanation does not seem adequate.

The information on the effect of moisture and of volatile acidic products upon the rate of deterioration is of considerable interest. The possible importance of the effect of moisture and volatile products upon oil-impregnated insulation was early recognized in the research project on insulating oils and cable saturants at the Massachusetts Institute of Technology, and for this reason these studies were made in a closed system which allowed retention of moisture and volatiles if desired. However, no concrete data, especially on paper strength such as given by Mr. Clark, were available. From a practical standpoint, I am glad to see additional evidence emphasizing the importance of keeping the content of moisture and acids in impregnated insulation to a minimum.

Mr. Clark's discussion of the effect of oxygen may create the impression that, above 120 degrees centigrade, oxidation becomes less pronounced while, if I understand correctly, oxidation above 120 degrees becomes less important only in comparison with the rapidly increasing pyrochemical decomposition.

REFERENCE

1. LOAD RATING OF CABLE—II, **Herman Halperin.** AIEE TRANSACTIONS, volume 61, 1942, pages 930–42.

J. G. Ford (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): I am very much interested in the paper of F. M. Clark on the subject of "Factors Affecting the Mechanical Deterioration of Cellulose Insulation." The paper indicates that he has done considerable detail work on this study of a problem which is very important to the electrical industry. There are several points which come to mind which I would like to discuss briefly.

In the method used by Mr. Clark as described in his paper the initial samples of 0.003 manila paper were conditioned at 65 per cent relative humidity at 70 degrees Fahrenheit. I note that the oil impregnated papers are tested immediately after being taken from the oil sample. This brings up the question as to the dryness of samples and the effect of the oil on the tensile strength on which aging tests have been made. It would appear that it would be desirable to test the samples all at the same water content so that the values obtained would be representative of the actual change in tensile strength. This, of course, would involve extracting the oil from the paper and subsequently humidifying under the same condition.

I note that practically all of the work that has been done on the subject of aging of cellulose insulation as used in transformers, involves tensile-strength measurements on the insulation. Personally I do not believe that deterioration in tensile strength tells the complete story, and I would suggest that compression strength as well as shear strength would be of even greater importance, particularly, since the short-circuit stresses on transformers subject the insulation to both compression and shear rather than tension. Based on actual tensile strength on old transformers we have examined, it would appear that such transformers would not stand any short-circuit tests. However, such transformers have been tested and found to stand up, again indicating that the insulation is more liable to fail by compression rather than tension. Of course, it is understood that lower tensile strength would also indicate lower compression strength of insulation, but this may not be a direct relationship. Mr. Clark has made a considerable number of tests to demonstrate the effects of decomposition products of cellulose on the deterioration of the cellulose. I think we all agree that decomposition products are detrimental and are a factor in the life of a transformer. However, I believe that the type of test Mr. Clark has made exaggerates the effect of decomposition products inasmuch as the cellulose is heated in a sealed tube in which all the components are at the same temperature, whereas in a transformer the winding temperature is always higher than the temperature of the oil and surrounding temperatures, and generally in any transformer there is a cool zone which will allow for the condensation of these products, thus reducing the effects. We have recognized the effects of reaction products from varnish and other materials as being also in the same category as decomposition products of the cellulose itself and have attempted to eliminate such products in the present design of the transformer. In many cases we have substituted varnish-treated materials with plain paper, and, where varnishes are used, we have adopted synthetic, low-acid-type varnishes, so as to keep the acidity at a minimum value.

I believe the study of insulation deterioration in transformers is very important and merits further study, but I am convinced that we should have compression and shearing strengths as well as tensile strength, and also that such laboratory information should be backed up by actual tests on aged transformers.

R. W. Atkinson (General Cable Corporation, Bayonne, N. J.): Mr. Clark's extensive data concerning deterioration of cellulose insulation at high temperature and his analysis of these data mark a great advancement in this important field. His quantitative information on the effect on deterioration at various temperatures of moisture and oxygen, singly or together, go far beyond anything previously available.

Over the past few years various manufacturers have been studying the deterioration of varnished cloth insulation at various temperatures. The very great increase of deterioration rate produced by the presence of small quantities of water has been found there also. Furthermore it has been found that this greater mechanical deterioration has been accompanied by a large increase in dielectric power factor. These results apply to sealed samples. In samples open to the air the moisture is driven off at the aging temperatures, and its effect becomes unimportant.

F. M. Clark: The study of the aging of any organic material is extremely difficult, because the products of the chemical changes involved are frequently extremely effective in determining the rate of subsequent deterioration. This has led to the generally accepted idea that the best way to study the deterioration of an organic product whether it be an automobile tire or mineral oil is to reproduce as nearly as possible the conditions of actual commercial use. This is true of cellulose insulation. Attempts to reproduce the conditions of transformer service, however, have resulted in a variety of test data which have been frequently in conflict and have been of doubtful value because of the difficulty of controlling the conditions of the test run. In general, therefore, the laboratory tests on the deterioration have been found to be most acceptable when some sacrifice has been made in the desire to reproduce commercial conditions in favor of the necessity of obtaining accurate test control. This has been done in the various studies both in this country and abroad in the testing of mineral oil and cellulose. Test results obtained in such a way have invariably been found to represent more severe deterioration than would be expected from service data but with proper consideration to the basic principles involved, such studies have promoted progress toward the goal of greater chemical and mechanical stability.

Mr. Halperin finds that the laboratory data which I have presented indicate a more severe deterioration than he has observed in cable studies. The test set-up which I have used was intended to produce data applicable to transformer use. Other studies which we have made indicate that the degree of deterioration observed under a definite set of test conditions is affected by the mechanics of the test set-up. This factor must not be ignored when one considers the commercial data presented by Mr. Halperin. I have not observed, however, that the differences in test set-up will alter the fundamental behavior involved, although they may affect the rate of deterioration. Thus despite wide differences in test set-up, the presence of moisture has always resulted in an acceleration of the mechanical deterioration in cellulose insulation.

It is true as stated by Mr. Halperin that the effects of oxidation do not disappear at temperatures above 120 degrees centigrade and I have tried to avoid any such impression. What I have tried to demonstrate is that, because of the importance of the purely thermal type of cellulose deterioration, chemical agents and mechanical means which might be effective in promoting the mechanical stability of cellulose at low temperatures where the oxidation effect is of importance must not be assumed to possess equal value in the stabilization of the cellulose at the higher temperatures obtained under overload conditions.

As indicated by Mr. Atkinson, the effect of moisture on insulation is of importance even when varnished materials are used. The relation between the chemical and mechanical changes in paper and the resulting power factor is a problem of commercial and technical importance.

I have followed the work of V. M. Montsinger in this field for many years. It is a pleasure to present data corroborating suggestions which he has made in the past and which have many times formed the basis for transformer design and operation.

I agree with Mr. Ford that other properties than tensile strength may be of value in the final analysis of those factors which affect the aging of cellulose. I find, however, that in a study of this type, limited objectives frequently result in faster progress than can be otherwise obtained. I hope that Mr. Ford or myself may sometime be able to present data covering the response of the compression and shearing properties of cellulose insulation to those factors which are discussed in this paper. With specific reference to the water content of the insulation when tested for tensile strength after aging, it has been our experience that the variation directly traceable to the difference in water content of the paper is of small magnitude in comparison to the total change described in the tests of this article. Attempts to follow a procedure such as suggested by Mr. Ford (extraction of oil and later humidification of the paper) have produced no worth-while results and frequently have raised questions directly related to the oil extraction effects. The work of Mr. Ford and his associates in this field has been of interest and value, and I hope that by the continued study of this problem in both of our laboratories we may contribute toward its ultimate evaluation and solution.

Application of Apparatus and Conductors Under Various Ambient-Temperature Conditions

Discussion and author's closure of paper 42-93 by R. E. Hellmund and P. H. McAuley, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, August section, pages 553–8.

V. M. Montsinger (General Electric Company): This paper gives some very valuable information on ambient temperatures throughout the United States and how they should be used. I am particularly interested in the author's derivation of the equivalent ambient obtained when integrating the area by the eight-degree rule. This should be factored under certain conditions in the overloading of transformers in cool ambients. The American Standard Guides now permit one per cent overload for each degree that the daily air is below 30 degrees centigrade for self-cooled, and the daily water temperature is below 25 degrees centigrade for water-cooled transformers. The guides also state that the load should be decreased two per cent for each degree that the daily ambient exceeds these values. The question has been raised as to what should be done in cases where the daily average ambient exceeds 30 degrees centigrade for a few days during the summer months. There are, no doubt, many days during the year in which the air is well below 30 degrees centigrade and where the one per cent overloading rule is not taken advantage of. In such cases it should not be necessary to derate the transformer during the few days when the average daily ambient exceeds 30 degrees centigrade.

The necessity for this derating can be overcome by increasing the ambient time period of 24 hours to one year. If the time period is increased to one year, the amount of overload should be based on the equivalent annual rather than the average annual ambient. The equivalent ambient is, of course, higher than the average ambient, since aging is not directly proportional to temperature. For example: by the eight-degree rule, six months' operation in a 30-degree-centigrade ambient and six months' operation in a ten-degree-centigrade ambient, is equivalent to 12 months' operation in 23.8 degrees centigrade or 3.8 degrees centigrade higher than the average value of 20 degree centigrade. Imposed on this, of course, is the difference between equivalent and average daily ambient, but this amount is small, being between 0.5 and 1.0 degree centigrade, and there is enough safety factor in the one per cent rule to take care of this difference. Hence, it is possible to use the daily *average* ambient instead of the equivalent daily ambient in applying the one per cent rule to daily overloads in cool ambients.

The use of the equivalent annual ambient will be advantageous in two ways.

1. It is a means—and apparently the only means—whereby the loading of transformers can be adjusted according to their annual ambient temperatures. In other words, transformers operating in the colder northern zone can be increased to give the same life as transformers operating in the warmer southern zone.
2. It will in many cases eliminate the necessity for derating during the few days in the summer months when the average daily ambient is above 30 degrees centigrade.

Whether the use of the average daily or equivalent annual ambient will permit the larger all-year output depends on local loading conditions. The conditions under which they should be used to determine the overload capability, are:

1. For one day emergency overloads, use the daily average ambient.
2. When the load is constant throughout the year, or when the greater capacity requirements are in the summer months, use the equivalent annual ambient.
3. When the greater capacity requirements are in the winter months, determine the equivalent ambient for the heavy-load months and for the light-load months, and check the load capability for both periods. This permits loading up to normal transformer temperatures in winter whenever the reduced summer load keeps the transformer temperatures to normal or lower on hot days.

The AIEE transformer subcommittee is studying the question of how to use annual equivalent ambients, and it is expected that its recommendations will be published in the near future.

P. H. McAuley: An error occurred in Figure 2 of the paper; curve 5 is for Helena and curve 6 for Bismarck.

Mr. Montsinger expresses agreement with the use of the equivalent annual temperature for determining apparatus loadings. In addition, he discusses operating conditions where it may be desirable to distinguish between daily and seasonal overloads. To obtain equivalent annual temperatures based on time-temperature curves as given for six cities in the paper is a rather laborious procedure and the essential data are not always available. C. F. Wagner has suggested an approximate method of determining the equivalent annual temperature. Only the average annual and monthly temperatures are used, such as given in the Atlas of American Agriculture, part II, section B, United States Department of Agriculture, 1928, for 626 cities. In this method aging units for each month at a given location are calculated with the eight-degree-centigrade rule and added to give annual aging units. A corresponding single temperature is found. From this calculated value the annual average temperature is subtracted. The difference is multiplied by 2 and added to the annual average temperature, which gives the equivalent ambient temperature. The factor 2 was obtained from a consideration of the equivalent temperatures for the six cities for which equivalent temperatures based on complete temperature-time curves are given in the paper. It is believed this method will give equivalent ambient temperatures in error not more than one degree centigrade. Because of the availability of the data required, this method has considerable merit.

Emergency Overloading of Air-Cooled Oil-Immersed Power Transformers by Hot-Spot Temperature

Discussion and author's closure of paper 42-115 by V. M. Montsinger and P. M. Ketchum, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, pages 906–16.

Nicholas M. Oboukhoff (The Oklahoma Agricultural and Mechanical College, Stillwater, Okla.):

PART I

The following remarks primarily relate to transformers whose load is either residential or combined.

While industrial load can be characterized fairly by a single typical load curve, this can hardly be done in the case of the loads just mentioned above; the reason is that industrial load is relatively steady throughout the year, but residential and combined loads vary considerably, being correlated in some way with the seasons, specific months, and so forth. Dealing with industrial load one can be justified in using *daily* load factor derived from a typical curve instead of *annual* load factor; the difference will be small anyhow. Likewise, the period of 24 hours can be assumed as a basic one; operations like the integration of hot-spot temperature curves over this span of time, conclusions, and results referred to it are all in order and legitimate.

Yet with residential or combined loads a single typical load curve is a rather artificial construction; therefore, the proposal is that several ones should be constructed according to the seasons, specific months, geographical location, industrial development, and so forth, as a case may require, while the whole year should serve as a basic period of reference. A co-operation of the operators in furnishing necessary data would be needed and should be secured.

All this is yet to come. In the meantime, however, it is possible to see that the character of conclusions and recommendations already reached and offered by V. M. Montsinger and other research engineers and workers will hold substantially—with an enlargement and more boldness in the application of their methods to the residential or combined load transformers, as soon as the year has been adopted as the basic cycle.

Holding always in mind that the discussion is centered about those transformers, a general substantiation of the above view is as follows:

There is a considerable difference between the highest load peak and the lowest one in the course of one year. The load curves as shown in Figures 1 and 2 of this discussion have been obtained on a local system by me in 1936; new data have been added in 1938 which disclosed that the highest load peak occurred on November 6 and amounted to 2,180 kw averaged over a half-hour, while May 9 of the same year the load peak was as low as 1,400 kw, which makes 64 per cent of the highest one, the difference being 780 kw. This is not a local feature; for other sources[1] give the ratio of the summer load peak to that in wintertime as between 0.57 and 0.70 for the same kind of load curves. Annual load factor was 0.44—also in agreement with reference 1, while daily load factor amounted to 0.53 in November and 0.67 in May, weekly load factors being 0.53 and 0.62 respectively. Those highs and lows were not accidental; they belonged to the most loaded week in November and to the least loaded week in May respectively, while other peaks were close to those designated before, that is, 2,180 kw and 1,400 kw during these weeks, respectively.

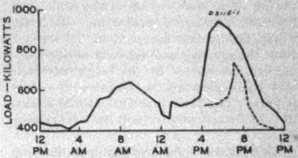


Figure 1. Residential and light industrial load curve

December 21, 1936
Maximum load 940 kw
Average load 595 kw
Ratio: average load to maximum 0.63
Ratio: rms current to maximum 0.65

Let us apply this situation to two cases:

First, assume that a transformer is in a continuous operation throughout the whole year; then it would be only consistent to apply the three per cent[2] rule to the *annual* load factor with the result that the permissible overload should be 16.8 per cent instead of 14.1 per cent during the high-load week and 11.4 per cent during the low-load one if the weekly load factors were used. The proposal is that the annual load factor be considered in determining overloads if transformers are continuously operated. As a matter of fact, the annual load factor is noticeably lower than the others for a residential or combined load; therefore, the suggestion is in the direction of a more liberal allowance.

Second, let us suppose that a bank of transformers is in operation, each transformer being uniformly loaded throughout the year; then a number of transformers simultaneously operated will be approximately proportional to a peak load with the result that it will depend on the seasonal or other variations of a load curve. In the given example approximately two thirds of transformers (exactly 64 per cent would be required to carry the peak for May 9, while full 100 per cent bank power would be used November 6; thus 780 kw would be in the reserve at the time it is needed the least. Because of the prominent normal peaks it is reasonable to expect overloading primarily during these peaks which at the same time limit the duration of overloading; the latter would hardly last longer than the former; thus, the severity and duration of overloading are determined by the height of peaks and their duration in addition to other factors. The lower are the normal peaks, the milder and shorter will the overloading be.

An available reserve of power at the time of the *lower* peaks would be manifested by the fact that only a part—the major one though—of a whole set of transformers would be in operation, while the others are idle; it would result in the prolongation of their commercial—not technical—life; certainly this would be an advantage under normal circumstances.

It may be questioned whether or not this advantage would justify itself at the present time; one may argue, on the one hand, that what matters now is not a prolongation of the commercial life of a transformer but rather the harnessing of its power reserve for an actual operation *now;* one of the possible ways of doing this would be to allow for such ultraoverloading at the time of the *higher* peaks that would limit or keep the commercial life from increasing, by affecting the technical life of a transformer involved; on the other hand, at the time when replacements might become difficult, a prolongation of the commercial life would be a valuable asset.

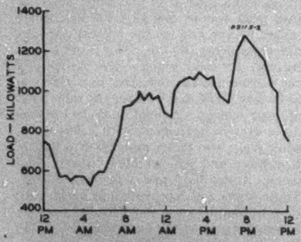


Figure 2. Residential and moderate industrial load curve

September 11, 1936
Average load 891 kw
Maximum load 1,275 kw
Ratio: average to maximum 0.70
Ratio: rms cur:ent to maximum 0.72

It goes without saying much that no finality is claimed as to specific values, examples, conclusions, and references given in the foregoing; they are offered to indicate general characteristics and trends and likewise call attention to a specific situation which is still fluid and not perfectly clear but already foreshadows the possibility of crystallization.

PART II

The implications of the discussed paper extend beyond the field of operation to that of design. Referring to the schedule of temperatures and Table VII, the discusser, for instance, reached the conclusion that standard design values of current density could be increased offhand by 18–27 per cent for transformers with 24-hour and 8-hour full industrial loads respectively. With standard design values of current density changed to higher values, cross-section areas of winding conductors will result in becoming smaller in inverse proportion, which means a considerable saving in copper; there will be more kilovolt-amperes per pound of copper or less copper per kilovolt-amperes.

The increased values of current densities thus revised are in agreement with the formula offered in my paper, "A Method of Successive Approximations in Electrical Design, Part II, Transformers," read before the Oklahoma Academy of Science at the annual meeting December 1937:

$$\Delta=\frac{622}{\alpha+0.035}\sqrt{\eta\frac{K_e}{K_s}} \qquad (1)$$

where

Δ stands for current density.
α stands for load factor.
η stands for core loss per pound.
K_e stands for price of winding conductors per pound.
K_s stands for price of steel laminations per pound.

As a companion formula the following one from the same paper is referred to:

$$A_s=C(\alpha+0.035)\sqrt{\frac{(\mathrm{KVA})}{4f}\cdot\frac{\Delta}{\eta B}}\times10^3 \qquad (2)$$

where

A_s stands for net steel cross-section area of a core.
C stands for output constant.
(KVA) stands for output in kilovolt-amperes.
f stands for frequency.
B stands for flux density in the core.

REFERENCES

1. ELECTRIC-POWER EQUIPMENT (second edition), J. G. Tarboux. McGraw-Hill Book Company, Inc., 1932, pages 33–4.

2. EFFECT OF LOAD FACTOR ON OPERATION OF POWER TRANSFORMERS BY TEMPERATURE, V. M. Montsinger. AIEE TRANSACTIONS, volume 59, 1940, November section, page 636.

W. C. Smith (General Electric Company, San Francisco, Calif.): The problem of permissible overloads on transformers, as viewed by operators and maintenance men, has too often been beclouded by two misconceptions:

> 1. That the 105-degree-centigrade limit for the hot spot is a magical figure above which the transformer will burn up immediately and below which one can operate indefinitely.
>
> 2. That if by chance a transformer has been subjected to a high overload with no failure resulting, this is a proof that such overloads can be applied more or less indefinitely without damage to that unit.

We now know that a time-temperature relation exists as regards the deterioration of class-*A* insulation, its life being halved for every increase of from five to ten degrees centigrade operating temperature. At the point of "zero life" the insulation has lost its "water of constitution," still retaining however adequate dielectric strength, but is so brittle that a slight mechanical strain will crack it and lead to electrical breakdown. Many operators have experienced the fact that it is dangerous to move to a new location their old transformers or regulators which have withstood high operating temperatures for many years.

The hot-spot temperature is the weak link in the chain. Every reduction of a degree in temperature, whether secured by various methods of artificial cooling or by a lowering of the effective ambient insures longer life. Wartime demands for increased power, together with the scarcity of materials, are now prompting a careful review of all factors which can be utilized to allow higher transformer loadings with minimum consequent operating temperatures. To the writer it appears that two important possibilities have been quite neglected in any papers or discussions on this question, reference being made to the provision of sunshades together with the use of nonmetallic paint.

Ambient temperatures normally discussed are government shade readings, and it is important to realize that the effective ambient for transformers in the sunshine may easily be 5 to 15 degrees centigrade hotter. I recall measuring the oil temperature, at Redding, California, of an idle 25-kva lighting transformer out in the yard, during the middle of a day when the shade temperature was 114-degrees Fahrenheit (45.5 degrees centigrade). The oil was 136 degrees Fahrenheit (57.8 degrees centigrade). Here was a difference of 12.3 degrees centigrade due to absorption of the sun's rays. A suitable sunshade, presumably a semicircular louvered wood structure on the south side of a transformer bank, could therefore be counted upon to insure several degrees (centigrade) lower operating temperature during the hottest part of the day.

The use of cable leads to the transformer greatly facilitates the placing of the sunshade. In the case of vertical drop leads, it will often be necessary to rearrange them to a 45-degree angle to place the shade up over the transformer, insuring protection from the noonday sun.

In the western states, at least, the use of aluminum paint for outdoor transformers is very widespread. A definite reduction in temperature rise should result from merely painting the north side of the tanks a dull black. A still further reduction will result if a sunshade is provided. Then it is very important to eliminate all aluminum-painted surfaces, and for this dull black is also the best finish. A further advantage at this time of the dull black is to serve as a camouflage.

To summarize, it is believed that over and above the possibilities offered by special forms of artificial cooling, a marked improvement in the maximum operating temperatures of transformers may be secured as follows:

1. Paint the shady side of all outdoor transformers a dull black.

2. Provide a sunshade for protection during the middle of the day and then paint the entire tank with dull black. Provide a dull black paint on all indoor banks of transformers.

V. M. Montsinger: Mr. Oboukhoff's suggestion of using annual instead of daily load factor for use of the three per cent rule under certain load cycle conditions is somewhat outside the scope of the paper, although it does have a direct bearing on permissible overloads at a time when all the overload capacity that is available should be made use of. No work has been done to determine how much could be gained by using annual load factors instead of daily load factors; consequently it is not possible to give a figure at this time.

Mr. Smith brings up two very good points, namely, that of providing sunshades, and that of painting the side of the tank not exposed to the rays of the sun a dull black. There is no question but that a sunshade would effect a material reduction in the temperature of a transformer exposed to the sun's rays. The amount of temperature reduction will depend on the size and contour of the radiating surface. For a small distribution transformer with a plain tank, the reduction could easily be 10 to 15 degrees centigrade, whereas, for a large tank having attached radiators or cooling tubes, the gain would be less since a smaller percentage of the total developed surface would be exposed to the sun. My experience indicates that the reduction in temperature would be in the order of five to seven degrees centigrade for large power transformers. If one per cent load can be gained for each degree reduction in temperature, then the use of sunshades should enable users to gain from 5 to 15 per cent in capacity. In reality we should say that the use of sunshades should prevent the derating of 5 to 15 per cent, since the sun causes the temperature to exceed its normal temperature rise. However, there is for most outdoor installations sufficient breeze to counteract a large part of the increase in temperature caused by the sun's rays.

In connection with Mr. Smith's suggestion of painting the north side of a tank a dull black, the effect of this also will vary, depending on the size and shape of the cooling surface. While it is not possible to give any calculated values of reduced temperatures, some idea of the benefits can be gained by the following statements:

1. A plain tank, with a metallic finish, will give approximately 30 per cent higher temperature rise than one having a nonmetallic finish.

2. As the surface becomes more and more convoluted, the effect of the metallic finish on the temperature rise of transformer in the shade decreases, it being from five to seven per cent for the most complicated surfaces. In the sunshine there is essentially no difference whether a metallic or nonmetallic paint is used, although, as pointed out above, the temperature rise will be greater for either kind of finish. This question is discussed in the AIEE paper entitled "Effect of Color of Tank on the Temperature of Self-Cooled Transformers Under Service Conditions," by V. M. Montsinger and L. Wetherill.

Reference

1. Effect of Color of Tank on the Temperature of Self-Cooled Transformers Under Service Conditions, V. M. Montsinger, L. Wetherill. AIEE Transactions, volume 49, 1930, page 41.

A New Single-Phase-to-Ground Fault-Detecting Relay

Discussion and author's closure of paper 42-125 by W. K. Sonnemann, presented at the AIEE summer convention, Chicago, Ill., June 22-26, 1942, and published in AIEE TRANSACTIONS, 1942, September section, pages 677-80.

J. H. Neher (Philadelphia Electric Company, Philadelphia, Pa.): While Mr. Sonnemann is to be congratulated on the development of this relay which distinguishes between single-phase- and two-phase-to-ground faults, he is to be especially commended for pointing out before the Institute the possibility of using this or other equivalent devices in conjunction with a simple instantaneous ground relay to obtain high-speed differential bus protection on systems grounded through a relatively high impedance.

Although systems of bus differential protection employing special nonsaturating current transformers have been recently introduced, these systems are difficult to apply where the single-phase-to-ground fault current is appreciably limited. The arrangement proposed by Mr. Sonnemann not only facilitates the application of these systems in this case but also offers a means of obtaining high-speed bus protection against single-phase-to-ground faults with conventional current transformers.

While the possibility of obtaining a solid three-phase bus fault by the inadvertant closing of a switch without removing safety grounds is always present, nevertheless the use of a high-speed single-phase-to-ground fault system will initiate tripping on the majority of faults before the system is rendered inoperative by the spread of the fault to another phase.

L. F. Kennedy (General Electric Company, Schenectady, N. Y.): The author of this paper correctly points out the severity of the problem encountered when applying bus differential protection to installations where the ground current is small because of high neutral impedance.

Other methods also have been applied to obtain the desired operation under these limiting conditions. In two of these other solutions which have been used, one uses a relay to recognize the difference between the two-phase-to-ground and single-phase-to-ground faults, and the other uses a relay system which makes selective control unnecessary.

The first of these uses another and simpler means of distinguishing a fault which involves only one phase from one which involves two phases. This method likewise depends upon the circuit characteristics encountered when a neutral impedance or relatively high value is used. If we look at figure 6c in this paper we note that the zero-sequence voltage is approximately 120 volts peak for a single-phase-to-ground fault and approximately 60 volts peak for the two-phase-to-ground fault. Associated with these voltage conditions will be approximately a 2/1 ratio in the neutral current. Hence a simple current relay may be used which will operate for single-phase-to-ground faults and not operate for two-phase-to-ground faults. Applications of this type of relay have been used to supervise differential ground relays since 1939. While it can be shown theoretically that such a relay may fail to operate due to impedance in the fault, in practice the value of the neutral impedance is so high that it is difficult to consider this as a very real limitation, when used for bus protection.

The other method of solving the problem in the case of these high neutral impedance systems is to set up each phase separately in a differential system, using a total of only three relays, each one being sensitive enough to operate for ground faults as well as for phase faults. With transformers such as the air-gap core type or the linear coupler there may be a small differential current for through phase-to-phase-to-ground faults due only to manufacturing tolerances. Operation under this condition can be easily prevented by using only a small amount of "through" current restraint and one can still easily obtain adequate sensitivity to provide positive operation for internal single-phase-to-ground faults.

For this type of application the restraining feature just described is a generally applicable solution, regardless of system impedance variations, and therefore more desirable. It is also a method following established practice and with proved reliability and simplicity.

R. C. Ericson (Northern Indiana Public Service Company, Hammond, Ind.): This paper has been particularly interesting, since it has presented us with another tool for combating false residual currents.

It is felt, however, that the relay has a wider application than the author has inferred in the paper.

The requirement that the ratio of the zero-phase-sequence impedance to negative-phase-sequence be of the order of two to one seems a little too conservative. A ratio of 1.5 to 1 should probably be sufficient in most cases.

Another impression obtained from the paper is that the relay is not applicable to solidly grounded systems. It would appear that the question of applicability rests wholly on the apparent ratios of the zero- and negative-phase-sequence impedances, as seen by the relay at the station in question.

The relay should also find some use in "direction comparison" pilot schemes (for instance, carrier-pilot relaying). This is particularly true of the schemes using the three-unit directional relay containing a voltage restraint unit and a polyphase and a residual power directional unit acting on one shaft. Schemes of this type very frequently employ a conventional residual current relay to remove the potential from the voltage restraint and polyphase power directional unit on the appearance of appreciable residual current. Experience has shown that under certain conditions this removal of potential is undesirable during two phase-to-ground faults.

A. R. van C. Warrington (General Electric Company, Philadelphia, Pa.): No relay session seems to be complete without a paper dealing with improvements in bus protection. For the past two years all efforts have been to improve current transformers. Now that saturation difficulties have been overcome by linear couplers and air-gap current transformers, the design engineers have again turned their attention to designing relays which will overcome the transformer errors still existing, in order to get still more sensitive bus protection.

The relay described by the author seems more complicated, however, than is necessary to do the job. A simple three-phase high-speed induction-cylinder undervoltage relay has been suggested, using two delta voltages and measuring the area of the station-bus delta-voltage triangle. The torque developed by this relay is proportional to the product of the two voltages and the sine of the angle between them. It can be shown that the area of the triangle during a single-phase-to-ground fault is essentially maintained during single-line-to-ground fault except with very low values of neutral impedance, while on phase faults or two-phase-to-ground fault the area will be drastically reduced, thus always assuring positive operation.

In the relay described in the paper the distinction between pickup on single-phase-to-ground faults and two-phase-to-ground faults disappears below a certain value of neutral impedance (R_n) which is one-third ohm in the example given. Below this value the relay torque reverses. Using the undervoltage relay scheme proper distinction is maintained even down to R_n equals zero. As a result it would appear that the three-phase undervoltage relay scheme using a simple high-speed induction-cylinder element is the simplest and most sensitive relay for this application.

W. K. Sonnemann: Mr. Neher's discussion goes one step beyond the points brought out in the paper and discusses the fact that a use of the new relay makes it possible to simplify the differential relays. The points made by Mr. Neher are well taken.

The author shows a ratio of 2/1 for a safety factor, but Mr. Ericson feels that a ratio of 1.5/1 would be sufficient in most cases. However, since the paper was presented, it has occurred to the author that, rather than apply the relay to the limits of some arbitrarily chosen safety factor, it would be better to determine the suitability of application by means of a few very simple calculations in the following manner: Consider an application in connection with bus differential protection. The relay should operate for a single-phase-to-ground fault on the bus. It should not operate for a two-phase-to-ground fault just outside the bus. Furthermore, it is possible that in some cases it should not operate for a two-phase-to-ground fault at the next outlying substation, depending upon the amount of current involved for the distant fault. It would therefore be in order to calculate the zero-sequence and negative-sequence voltages on the bus for the internal single-phase-to-ground fault and for the remote two-phase-to-ground fault as far removed from the bus under consideration as the application may require. Both of these calculations should include a reasonable value for possible fault impedance. Having obtained the voltage values for the two conditions, it is a simple matter to determine from the operating characteristic curve of the relay whether or not it is suitable. It may be that in some cases the curve shown by Figure 3 of the paper would not be suitable, but that some other curve for which the relay is capable of being adjusted would serve the purpose.

Mr. Kennedy mentions another method of discriminating between single-phase-to-ground and two-phase-to-ground faults by means of the relative magnitude of the zero-sequence current. This method is theoretically correct, but it is more than theoretically possible that such a relay may fail to operate because of impedance in the fault. The author knows of one case at least where the impedance of the fault was large enough to prevent the operation of such a relay.

The use of through current restraint has been and is quite practical and widely used. However, on multicircuit busses the complexity of using restraint certainly increases rapidly with the number of circuits. In view of this fact, the author feels that if the restraining coils of the differential relay could be eliminated by the installation of one additional relay as described in this paper, that the over-all installation would be simplified.

A. R. van C. Warrington mentions another method of using a relay responsive to the area of the station-bus delta-voltage triangle. This method has been considered by the author and his associates and considered to be more complex and difficult to predict than the method used in this paper. The area of the voltage triangle depends upon the negative- and positive-sequence voltages and the angle between them. The negative- and zero-sequence voltages at the bus are expressed as impedance drops from the source to the bus. If the positive- and negative-sequence voltages are used as Mr. Warrington would do, an additional factor is introduced: namely, the generated voltage. This enters in the positive-sequence network, and the determination of the relay characteristics is not as straightforward. It is difficult to understand Mr. Warrington's statement that the conventional relay and method he proposes appear to be simpler than the relay the author proposes. Actually, we feel that the reverse is true since no moving contact could be more simply actuated than by a spring supported armature without bearings. Furthermore, the sequence networks in the new relay are straightforward and of proved design.

In the oral discussion at the convention, the author pointed out that the relays could be utilized on systems where the grounding impedance is even less than the one-third ohm Mr. Warrington indicates in his last paragraph by the simple expedient of reversing the operating and restraining coils. However, we feel that this point is actually only of academic interest in that the need for the relay disappears on solidly grounded systems.

Protection of Pilot-Wire Circuits

Discussion and authors' closure of paper 42-136 by E. L. Harder and M. A. Bostwick, presented at the AIEE summer convention, Chicago, Ill., June 22–26, and published in AIEE TRANSACTIONS, 1942, September section, pages 645–52.

R. C. Ericson (Northern Indiana Public Service Company, Hammond, Ind.): The authors are deserving of some praise for their able presentation of the subject of pilot-wire protection. The material presented should prove of value not only to those engineers involved in designing pilot relay circuits, but also those working on communication, remote metering, or supervisory circuits.

W. R. Brownlee (The Commonwealth and Southern Corporation, Jackson, Mich.): The use of pilot-wire protection has made possible the successful design and operation of power-supply systems with significant savings in major equipment. The use of this valuable tool has not increased as rapidly as might be expected, and possibly the greatest retarding influence has been the feeling that sufficiently reliable pilot-wire circuits cannot be secured. Many years' experience with pilot-wire protection[1,2] (principally with leased circuit) have shown that an excellent degree of over-all performance of pilot-wire systems may be secured without requiring anything like 100 per cent continuity of the circuit, providing adequate continuous circuit supervision is included. However, the pilot-wire circuit must be protected from even momentary failures which by their very nature coincide with power-system trouble which may be on the circuit protected. There is still considerable confusion regarding the influence of power-system faults on pilot-wire circuits, and therefore the theoretical analysis of the authors is most timely.

What is believed to be the first installation in the United States of neutralizing transformers to protect pilot-wire circuits against rise of station ground potential was made in 1934 on the system of the former Tennessee Electric Power Company, and it was checked thoroughly by staged short-circuit tests in February 1935.[2] Many installations have since been made of three-winding neutralizing transformers to protect against rise of station ground potential, and for that purpose, these appear to have some important advantages over the two-winding capacitor excited transformers now offered.

1. The three-winding unit provides genuine "neutralization" (see Figure 9 of paper) of the voltage rise so that terminal equipment and pilot-wire cable are unstressed, whereas the two-winding device acts as a voltage divider, resulting (Figure 9c) in impressing some 1,000 volts on the pilot-wire cable insulation for a total rise of only 2,000 volts. Protective devices connected between the conductors of communication-type cable and the sheath will operate to short-circuit and ground the pilot wires at such voltages. It is doubtful if this difficulty can be overcome by the addition of capacitors without introducing other complications.

2. If connected as shown in Figure 6 of the paper, the magnetizing capacitors become equivalent to 0.5 microfarad connected across the circuit at each terminal. With even reasonably short pilot-wire circuits, the cable capacitance will be of the same order, so that the total will be beyond the maximum recommended for use with certain two-wire differential-type pilot-wire schemes.

3. It is possible that the two-winding transformers and capacitors are subject to transient effects such as were encountered with an early design of transformer which was built to neutralize induced voltages.[3] Actual fault tests on this unit showed an initial unneutralized voltage for the first cycle which was nearly as great as the total unneutralized voltage. Thorough short-circuit tests should provide a definite answer, and, if these had been made, the results should be of interest.

The authors' statement that no specific protective measures are required in many cases is true but should be used with considerable caution. For example, it is sometimes thought that the connecting of the power-station ground networks and the communication-system protective grounds to the same water system obviates the need of protection. Short-circuit tests have disclosed as much as a 2,000-volt drop between the water service connection at a power station and remote ground caused largely by the impedance of the meter and service connection.

The authors mentioned an effective primary impedance of a three-winding neutralizing transformer as some 11,000 ohms compared with 100,000 ohms for the two-winding transformer. Several installations have been made of three-winding neutralizing transformers with a primary impedance exceeding 100,000 ohms at 4,000 volts, 60 cycles.

Is circuit neutralization achieved by the connections of the two-winding transformer shown in Figure 6, or must the insulating transformer of Figure 8 be used? If Figure 6 is adequate, then the need for the insulating transformer of Figure 8 is questionable.

References

1. The Use of Communication Facilities in Transmission-Line Relaying, J. H. Neher. AIEE Transactions, volume 52, 1933, June section, pages 595–602.

2. Pilot-Wire Relay Protection, E. E. George, W. R. Brownlee. AIEE Transactions, volume 54, 1935, November section, pages 1262–9.

3. Neutralizing Transformers. Edison Electric Institute publication *H*-12.

K. H. Perkins (Bell Telephone Laboratories, Inc., New York, N. Y.): Neutralizing transformers to prevent extraneous low-frequency longitudinal voltages from appearing on communication or signaling circuits have been in use for a long time and have formed the subject of a number of papers before this Institute. The device which the authors refer to as a two-winding neutralizing transformer might more properly be called a new name for an old tool: namely, the longitudinal choke, under which humble label it has been known for a long time. To regard this choke as a neutralizing transformer may possess advantages when comparing this device with the conventional type of transformer. The most interesting and valuable parts of the paper are the sections devoted to methods for computing the distribution of voltage along the protected conductors.

It should be pointed out that pilot wires are frequently in a cable containing other communication or signaling circuits. The type of service on these accompanying circuits may be such that momentary interruptions, such as might be occasioned by the operation of lightning arresters or protectors during fault conditions on the power system, are not serious. However, it is necessary under these conditions to make certain that the insulation between the pilot-wire pairs and the other conductors is not stressed dangerously, because breakdown of this insulation might result in failure of the pilot-wire circuits. It may therefore be necessary in a situation of this kind to provide all of the circuits with the same kind of protection as the pilot-wire circuit. Many different situations, in which this question of potentials between conductors is involved, may be encountered in practice, and each situation must be worked out individually. It is intended here merely to point out that in order to protect the pilot-wire circuits themselves, attention may have to be given to other circuits in the cable besides the pilot-wire conductors.

In the section dealing with the three-winding transformer, it is implied that the potential of remote earth is reached a few hundred feet from the station ground. While it is true that in most cases the largest part of the potential between the station ground and remote ground takes place within a few hundred feet, frequent cases have been encountered in which it was advisable to carry the primary connection out to as much as several thousand feet. In commercial telephone circuits, the remote ground must be located far enough from the station ground so that the remanent voltage will not cause operation of protector blocks. Local conditions determine the potential distribution, and it is advisable to fix the position of the primary ground by test.

In the discussion of the two-winding transformer, it is stated that the latter is more effective than the three-winding transformer, because neutralization is aided by the exciting current in the case of the two-winding transformer and hindered by it in the case of the three-winding transformer. This comparison has no very great significance, because many other factors enter into the relative merits of the two types of transformer, and by proper design either can be made as effective as desired. By admitting the exciting current to the signal circuit itself, rather severe balance requirements are placed on the longitudinal circuit and upon the grounding capacitors. Otherwise, the flow of longitudinal current may give rise to metallic voltages which might interfere with the relay operation. Also, since capacitors are used to provide a path to ground for the exciting current, the device is restricted to circuits using d-c or low-frequency signals.

In the discussion of potential distribution resulting from combined station-ground potential rise and magnetic induction, it is stated rather positively that the voltages produced by these two effects are 90 degrees out of phase. It should be recognized that this is only an approximation. Actually, the phase angle between these voltages depends upon the earth resistivity and the separation between the power circuit and the signaling circuit. For example, at a separation of 200 feet and an earth resistivity of 100 meter-ohms, the phase angle is about 65 degrees.

L. L. Fountain (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): This paper gives information for which there has been a definite need for some time. This need has been intensified by the rapidly increasing use of the pilot-wire relay. The information given in the paper should serve to prevent the recurrence of difficulties which have been encountered heretofore caused by a lack of understanding of the pilot-wire requirements with respect to induced voltages and station ground potentials. Investigation has shown that, in practically every case, proper pilot-wire protection results in satisfactory relay operation.

In the closing paragraph of the paper, reference is made to leased pilot wires. While there is probably no place in the paper for further amplification of this subject, there is a definite need for more information on the characteristics of the available pilot-wire circuits that can be leased and the conditions under which they may be made available.

The authors make mention of the fact that "the seal-off voltage of the arresters must be above the maximum voltage that can be impressed on them by induction in the pilot wires, and so forth." This point deserves special emphasis. Since leased circuits have a fixed insulation level and utilize carbon protector blocks (arresters) to suitably protect the cable insulation, little variation is possible in these facilities. It is, therefore, essential to determine that the longitudinal induced voltage to which the pilot-wire pairs may be subjected is below the breakdown value of the carbon blocks in order to determine whether or not the circuit is suitable for use with pilot-wire relays. In the past, a few isolated cases of trouble have occurred because of the failure to observe this precaution; however, it is believed that continued co-operation between the telephone company, utilities, and electrical manufacturers will result in a better understanding of the conditions and prevent the recurrence of this phenomenon.

The method of presenting this difficult subject has been admirably handled, and the authors should be commended for their effort.

E. L. Harder and **M. A. Bostwick:** Before commenting on any of the discussion, the authors wish to express their appreciation of the interest shown in this paper. It was prepared to review the problem of protecting pilot-wire circuits, calling attention to methods that can be used to determine the distribution of voltage stresses that are imposed on those circuits, and to present a new type of neutralizing transformer that is of value for protection of these circuits. The discussion of the relative merits of different types of neutralizing transformers was merely incidental, since the primary object of the paper was to call attention to the problem of protecting pilot-wire circuits. Once that problem is appreciated, it is assumed that inherent difficulties, that may have been previously overlooked, will be avoided.

In reply to Mr. Brownlee's discussion we agree that each type of neutralizing transformer, three-winding or two-winding, has certain advantages over the other. As pointed out in the paper, however, it will be noted that the two-winding neutralizing transformer is particularly well adapted to the protection of pilot-wire circuits, which are subject to both induction and differences in ground potential. The "voltage-divider action," cited under the first paragraph of Mr. Brownlee's discussion, and illustrated in Figure 9c of the paper, is only incidental and was illustrated in the paper to guide users so as to avoid the condition. Figure 9b illustrates the correct use of the two-winding neutralizing transformer. The required capacity between the pilot-wire circuit and ground may be provided either in the form of the natural capacity of the circuit or by adding lumped capacity. In either case the required capacity is well within the operating range of a-c pilot-wire relays that are now on the market.

Figure 6 of the paper was used to illustrate schematically the proposed relay circuit. Actually this circuit is made up as shown in Figure 4. Since the 0.5-microfarad capacitors are connected between mid taps of the insulating transformer and ground, it will be noted that this capacity is not introduced into the relay circuit. Consequently, the operating limits of the associated pilot-wire relay are not affected by this connection.

The circuit tests that are suggested in Mr. Brownlee's third paragraph have been in effect performed by several years experience in operating pilot-wire circuits identical with those described in this paper. A consideration of the circuit will reveal that the transient effects questioned are only of interest when they are introduced into the pilot-wire loop. Normal action of the two-winding neutralizing transformer is to introduce a high impedance into the series circuit that is made up by paralleling both pilot wires and connecting them to ground through the terminating capacitors. Since this circuit must be symmetrical—that is, equal impedances are introduced into both pilot wires—, it will be noted that any transient disturbance set up by transmission-line faults will not introduce extraneous voltages in the pilot-loop circuit. Consequently, the effect which Mr. Brownlee has feared, is not encountered. This is borne out by actual operating experience.

Adequate neutralization of all voltages, within the operating range of the two-winding neutralizing transformer is obtained by use of the circuit which is schematically illustrated in Figure 6. The insulating transformer shown in Figure 8 is not required, as the residual voltage on the relay side of the neutralizing transformer is in the order of 1.3 per cent of the applied voltage. However, the circuit illustrated requires the ratio change and mid tap connection that is provided by the insulating transformer shown in the diagram. Since this is standard apparatus, the insulating transformer is used rather than supplying an additional device.

We agree with Mr. Perkins' statement that the two-winding neutralizing transformer has been previously used as a "longitudinal choke." While these two pieces of apparatus are of similar construction, the actual design and use differ quite radically. The device which we have described is of particular value for protection of pilot-wire circuits, while the "longitudinal choke coil" was designed especially for communication circuits.

We appreciate having Mr. Perkins call our attention to the fact that voltages between wires in a given cable must be considered. This is most important, and the problem must be studied to avoid the possibility of transferring voltage stress from one circuit to another within a given cable. As pointed out in a paragraph that was added to the paper for publication in AIEE TRANSACTIONS, "generally, all wires entering a cable at a given point should be protected in a similar manner." We also agree with Mr. Perkins' statement that it is advisable to fix the position of the primary ground by test rather than relying upon the assumption that average earth potential will be found a few hundred feet removed from station ground. The fact that the station ground potential may be less than 90 degrees out of phase with the longitudinally induced voltage was neglected, as a quadrature addition of these voltages is sufficiently accurate for the purpose of this discussion.

As both Mr. Perkins and Mr. Brownlee have pointed out, the design details of the different types of neutralizing transformers discussed in the paper are not fixed. The impedance of either design may be changed to suit the particular application. For example, the shunting action of the exciting capacitors, used with the two-winding neutralizing transformer can be reduced by grounding these capacitors through a mid tapped reactor. The impedance of this reactor can be chosen so as to avoid shunting high-frequency currents out of the pilot circuit and yet to shunt low-frequency currents, such as third harmonic currents, if this is considered desirable.

Current- and Potential-Transformer Standardization

Discussion and author's closure of paper 42-116 by the AIEE current transformer subcommittee of the committee on protective devices presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, September section, pages 698–706.

L. F. Kennedy (General Electric Company, Schenectady, N. Y.): The interest of the relay engineer in these standards will be largely concerned with the section covering the specification of accuracy classes for relaying service and the application data to be made available.

In connection with the method of specifying overcurrent performance for example $10L400$ or $2.5H200$ it should be remembered that this subject has been under active consideration for many years. During this time many suggestions have been made as to how this specification could be given in such a way as to convey the maximum information. From these several suggestions either or both of two points always seemed to be significant. One was the realization that current-transformer errors increase rapidly after reaching the ten per cent value. The other was the desire to know the performance at 20 times current. Your working committee has succeeded in combining these two ideas in a specification which tells the voltage (or burden) permissible without exceeding a ten per cent error at 20 times secondary current. For those who prefer a burden figure to a voltage figure, it may be in order to emphasize that the last number in the accuracy specification divided by 100 gives the permissible burden in ohms.

Incidentally, I believe the committee should be congratulated for finally establishing ohms as the basis of expressing burden in preference to the volt-ampere expression which was almost meaningless for relay work where operating values other than five amperes are so frequent.

The section on application data recognizes the usefulness of the excitation characteristics as a means whereby the actual error under specific conditions may be accurately determined. It would appear that the best method of presenting and using these data has not been established. Three methods of calculation are indicated in Appendix 2, and figure 5 shows a graphical solution. The text indicates that the published information should include excitation current versus voltage and "sufficient data to determine" inphase and out-of-phase current, or phase angle. Since all of the indicated methods use either φ, directly or the components of I_e which are obtained by knowing φ_e, it would seem to be in order for the committee to definitely recommend that the angle of the exciting current together with the magnitude constitute the basic data.

E. C. Eberhardt (Commonwealth and Southern Corporation, Jackson, Mich.): The paper should be very helpful in giving a better understanding of section 4 of the revised American Standard for Transformer, *C*-57 when it is published. The latter part of the paper obviously reproduces certain parts of section 4 as now revised, although it is not clearly identified as such in the paper. Including this material for reference permits a better understanding of the discussion in the paper and also makes this information generally available in advance of the publication of *C*-57 which should be helpful to those interested in this equipment.

"Standard Accuracy Classes for Current Transformers for Relaying Service"

The paper clearly states the purpose of these accuracy classes for relaying service; namely, they provide a bench mark for comparison and for specifying the overcurrent accuracy requirements and establish in a general way the limits within which a given transformer does not exceed specified errors. That is, the method of classifying the accuracy is standardized, but no recommended or preferred classifications for standard production units have been indicated either by this paper or in that part of section 4 reproduced in the paper. The considerable number of accuracy classifications in the standard are required in order to provide the necessary "bench marks" for standard production units and for special requirements. Another important function of standardization might readily be accomplished by establishing specific accuracy classes as standard or recommended for certain general applications in which current transformers are widely used. Thus wound-type current transformers suitable for applications such as the general requirements of electric-power stations, both for metering and general relaying, might for example have a recommended accuracy classification of 3/10 *B*1/10, 3/10 *B*5/10, 3/10 *B*2; 10*H*100, 2.5*H*100. Such emphasis on specific classifications should serve as an incentive toward standardization in specifying accuracy requirements and for uniformity in standard catalogued transformers.

Under 4.032 (*a*) (2) of the proposed revison of *C*-57 it is specified that a current transformer shall be given an accuracy rating in accordance with the maximum secondary terminal voltage at which the specified error will not be exceeded under the conditions specified for rating purposes. Apparently the intent of this requirement is to insure that the accuracy rating shall be a true bench mark of the unit's ability. That is, where a standard available design having an accuracy rating of 10*H*100 is furnished to meet a specified accuracy class of 10*H*50, it shall be given its rating of 10*H*100 and not the lower rating, which could happen under the maximum error-limit requirement only. It is commendable that this provision has definitely been included in the standard so as to avoid possible confusion and misunderstandings.

MECHANICAL LIMIT FOR CURRENT TRANSFORMERS

The paper provides an interesting discussion on the revised method of expressing the mechanical limit of current transformers and also indicates test conditions by means of which the completely offset requirement may be demonstrated. Since the stated tests are equivalent to the completely offset condition being maintained for six cycles and also definitely establish a crest value of not less than 2.82 times the rated mechanical limit expressed in symmetrical rms amperes, they represent the extreme in completely offset requirements and hence should demonstrate the unit's ability without question. However, the standard does not specify this demonstration, and the paper merely reads "may be demonstrated." Unless the above demonstration or some other common basis is definitely recognized, so that the mechanical limit rating indicates a definite ability, the present confusion as to what is meant by the claimed ability will continue to exist.

The proposed standard specifies that the symmetrical rms mechanical limit rating shall not exceed one-half the thermal limit of six cycles. Such a requirement would be necessary to provide adequate thermal ability for a demonstration test such as that indicated in the paper.

110-KV INSULATION LEVEL

Tables IX and X of the revised standard make the new 110-kv basic insulation level applicable to instrument transformers in insulation class 15*H*. This should permit appreciable savings where instrument transformers are desired having an impulse rating comparable to the power level in the 15-kv insulation class. Formerly, it was necessary to go the 150-kv impulse-rated units or purchase special nonstandard units. It is interesting to note a definite trend in catalogued equipment toward adoption of the 110-kv impulse rating instead of the 95-kv level for 15-kv outdoor units of certain types.

F. E. Davis (The Commonwealth and Southern Corporation, Jackson, Mich.): It seems to me that metermen, in general, should be most interested in and pleased with the fact that we now have a standard method for rating instrument transformers that is of greater significance from a metering standpoint than the method formerly used. Formerly, only arbitrary limits for ratio error and phase angle of the transformers were used for rating purposes. Now, arbitrary limits for the effect of the transformer ratio error and phase angle on the accuracy of measurement are used, and this is the factor with which metermen are primarily concerned. Thus, the ratings given transformers on the new basis define closely the limits of the extent to which the transformer characteristics affect metering accuracy.

I might also call attention to the fact that, in rating current transformers for relaying service by the new method, it is necessary to rate them both under the 2.5 per cent and 10 per cent specification (such as 2.5 *H* 50 and 10 *H* 200). This is for the reason that a rating under one condition is not indicative of the performance under the other in all cases. (For instance, a 2.5 *H* 200 rated transformer would also meet the 10 *H* 200 requirement, but the reverse is not necessarily true. Both, therefore, should be specified.)

S. D. Moreton (General Electric Company, Philadelphia, Pa.): The proposed American Standard for Current Transformers requires that the manufacturers now give the excitation characteristics of the transformer. This makes it possible for the customer to calculate the transformer errors for his particular application resulting in better system co-ordination than was possible heretofore when interpolation between the standard ratio curves was the only method available aside from actual test.

The authors of this paper have mentioned several methods for the calculation of ratio correction factor and phase-angle errors of current transformers. I wish to stress that any mathematical method will be long and tedious unless one is willing to accept the inaccuracies of adding the magnitude of the exciting ampere-turns arithmetically to the secondary ampere-turns to obtain the primary ampere-turns. The chart method as mentioned in appendix 2 under "Method 2" as shown in figure 5 of the paper is in reality a vector method for the addition of the exciting ampere-turns and the secondary ampere-turns to obtain the ratio correction factor and phase-angle (β) directly. This vector method is the ideal answer for rapid and accurate calculations: by the use of vector charts of this nature calculations may be made in one-fifth of the time required for mathematical analysis, with an accuracy that is far superior to slide-rule work.

An example would perhaps show more fully the labor that is avoided by use of the chart method. Let us assume that the value of the exciting current has been determined from the burden requirements:

as $\bar{I}e = 5\sqrt{60} = 2.5 - j4.33$
where $\bar{I}s = 25\sqrt{30} = 21.65 - j12.5$

The following steps must be performed if method 1 in Appendix 2 is used:

$$I_p = \sqrt{(21.65+2.5)^2 + (12.5+4.33)^2}$$
$$= \sqrt{(24.15)^2 + (16.83)^2} = \sqrt{583+284}$$
$$= \sqrt{867} = 29.4 \quad \text{amperes}$$

$$\text{RCF} = 29.4/25 = 1.175$$
$$\beta = (\tan^{-1} 16.83/24.15) - 30 \text{ degrees}$$
$$= (\tan^{-1} 0.698) - 30 \text{ degrees}$$
$$= 34.9 \text{ degrees} - 30 \text{ degrees} = 4.9 \text{ degrees}$$

The mathematical addition of two vectors requires that squares and square roots be taken as well as the use of trigonometric functions in relatively complicated formulas as indicated by the authors' methods; these calculations are especially laborsome if numerous points are required as for curve drawing.

The steps involved in performing the calculations by the use of vector charts are

Per unit exciting current $= \bar{I}e/\bar{I}s$
$= 5\sqrt{60}/25\sqrt{30} = 0.2\sqrt{30}$

Using the chart given in figure 5 of the paper the ratio correction factor and the phase-angle error (β) are determined directly as:

$\text{RCF} = 1.177$
$\beta = 4.85$ degrees

By the use of the vector-chart method 80 per cent of the time can be saved, greater accuracy obtained, and the calculations readily made. Thus a simple and rapid means for calculation, that does not lose any accuracy through the process used to obtain the result, is the one that should become the predominant method—the vector-chart method.

C. A. Woods, Jr. (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): 1. This report covering the recent work of the current transformer subcommittee gives some insight into the vast amount of work involved in obtaining a set of standards which meet present-day requirements without any material conflict with past practices. The discussions and explanations on the revisions and new material of these standards gives the user of this apparatus a better picture of the characteristics and limitations of instrument transformers.

2. The new sections covering current-transformer accuracies and application data are of particular interest to the application engineer. The use of the saturation or excitation curve made from data measured on the current-transformer secondary winding with the primary winding open-circuited for determining performance is emphasized. It might have been well to point out, that when a satisfactory method of determining and expressing the secondary leakage reactance of current transformers has been accepted, the standards could dispense with the use of ratio and phase-angle errors as a means of accuracy classification of overcurrent performance. Then, excitation characteristics could be used exclusively.

3. Appendix 2 gives 3 methods of calculating ratio correction factor and phase angle from the magnetizing and loss characteristics. As stated, all three methods are simply different means of obtaining the solution of the vector diagram Figure 3. Method 1 is the fundamental solution and most readily followed. Method 2 has its principal interest in its accompanying chart, Figure 5. Amplification of the use and limitations of this chart would be of interest. Method 3 is another form of the expressions given in method 2. It would seem method 1 is so simple and fundamental that it would be the desired method, eliminating the inconvenience of requiring special charts or formulas to be always available. Likewise, it is a constant reminder that the ratio of the excitation current, I_e, to the load current, I_2, is all that is required for a large percentage of applications.

4. It is felt this report will stimulate the use of the new standards and further investigation of transformer performance.

J. E. Clem (chairman of committee): The members of the committee are gratified that the discussions have been so complimentary and so constructive.

Mr. Moreton expresses a strong preference for the chart method of determining the performance of current transformers in the overcurrent field, while Mr. Woods expresses a strong preference for method 1. It is hoped that those who use any of the methods given for calculating overcurrent performance will compare the various methods carefully and express an opinion so that the committee may recommend a preferred method for inclusion in *C*-57 as an addition to the test code.

Likewise Mr. Kennedy's suggestion that the exciting current data required be on the basis of magnitude and phase angle is also a matter on which an expression of preference is desirable.

Mr. Eberhardt suggests that specific accuracy classes be established as standard or recommended for certain applications. Such material does not belong in a standard of this nature. Such information might take the form of an industry report and, if a nearly unanimous opinion could be obtained, included as an appendix to the standard under the heading "Guides for Application."

Mr. Eberhardt notes that the standard does not require that the mechanical limit of current transformers be demonstrated. Actually the standard does not require that any of the characteristics be demonstrated, either in section 4 or in the other sections of *C*-57. The standard requires that the apparatus have certain characteristics, and whether or not tests are made to demonstrate that the apparatus fulfills the requirements is a matter of manufacturing practice and expression of customer's wishes.

Mr. Davis suggests that current transformers be given an overcurrent rating at the 2.5 per cent point as well as the ten per cent point. This is not practical, because the second point would not really be indicative of the performance of the transformer. Because of the fact that the voltage steps are multiples of 2, starting with 50, the voltage corresponding to 2.5 per cent would be between the standard voltage values, and any rating for the 2.5 per cent accuracy would therefore be misleading.

Study of Driven Rods and Counterpoise Wires in High-Resistance Soil on Consumers Power Company 140-Kv System

Discussion and authors' closure of paper 42-107 by J. G. Hemstreet, W. W. Lewis, and C. M. Foust, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, September section, pages 628–34.

J. R. Eaton (Naval Training School, Purdue University, Lafayette, Ind.): The studies described in this paper show that if lightning strikes a tower equipped with both buried counterpoise wires and deep-driven ground rods, the resulting current in the ground rods is much greater than the current in the counterpoise wires. From this behavior, it is logically concluded that the deep-driven rods provide a more effective ground than do the counterpoise wires.

In comparing the merits of the driven ground rod and the counterpoise wire in the light of the performance observed in Michigan, it may be well to call particular attention to one peculiarity in the geology of the region in which the studies were made. As implied by conclusion 2, the high-resistance sandy soil of this region is underlaid by a different kind of soil (presumably clay) which is of relatively low resistivity. Rods driven deep into the sandy layer provide a relatively high-resistance ground connection. When such rods are driven sufficiently deep that the underlying clay layer is penetrated, the resistance drops rapidly, indicating that by far the greater part of the conductance is through the clay soil. The superior performance of the deep-driven ground rods as observed in the Michigan tests is very likely principally because of the fact that the rods make contact with the highly conducting soil layers considerably beneath the surface.

It must not be concluded from these tests that deep-driven rods will in every case be superior to horizontal buried wires. Only in case the deep rods penetrate lower-resistance soil layers below the surface, can the rods with certainty be considered as superior to the buried counterpoise wires.

S. K. Waldorf (Pennsylvania Water and Power Company, Baltimore, Md.): Hemstreet, Lewis, and Foust have evaluated the relative merits of rods and counterpoise as collectors of lightning current. What must of necessity be of prime importance to transmission engineers is the relative effectiveness of these two types of auxiliary grounding in the prevention of flashovers. It will perhaps help answer this question if the authors in their tabulations of footing resistances and tower currents would indicate which were associated with relay operation and where evidence of flashover was found. In other words, do the data give any indication that rods or counterpoise are the more effective in the prevention of flashovers?

Hamilton Treadway (United States Department of Agriculture, St. Louis, Mo.): The authors have conducted a most interesting and enlightening study of the characteristics of grounding methods under actual field conditions. This paper is a most valuable contribution to the literature on this subject.

A point of interest on which the data do not shed light is the time-impedance characteristic of the ground electrodes during the lightning stroke. The methods of measurement of course did not lend themselves to obtaining such characteristics. However, the existence of a high initial impulse impedance has been a point of some controversy although established by theoretical analysis. It is unfortunate that the data presented do not enlighten us on this point. It is, of course, needless to point out that the existence of an initial impulse impedance in excess of the Megger values would result in some instances in potentials much greater than the flashover characteristics of the line. Of course, such voltages would exist only for a few tenths of a microsecond. The opinions of the authors on this point are desired.

The very low ratios of surge impedance to "normal impedance" is to be expected in high-resistance soils. As has been previously suggested either the counterpoise or driven ground rod is essentially an imperfect capacitor in series with a resistance and inductive reactance. The contact resistance between the soil and the grounding electrode is paralleled by the capacitance of the rod. During the surge the contact resistance is effectively shunted out. Therefore, where higher "normal" impedances are indicative of high-contact resistances accompanied by large rod capacitance, the ratio of impulse impedance to the "normal" impedance will be quite low. This, of course, means that the true criterion of the effectiveness of the ground is the leakage resistance. In other words, the "normal" or d-c or 60-cycle impedance measurements are of value only insofar as they indicate the magnitude of the leakage resistance. It is probably true generally that low "normal" impedances indicate low-contact resistances and high "normal" impedances indicate high-contact resistances. This is probably also true of the leakage resistance.

It is believed that leakage resistances will be reduced most effectively by deep grounding. This is indicated by the data presented and the conclusions of the authors.

J. H. Hagenguth (General Electric Company, Pittsfield, Mass.): This paper is of considerable interest because it gives results of lightning strokes to lines with unusual grounding conditions. The knowledge gained by this and similar investigations has helped considerably to improve the lightning performance of transmission lines. I believe that considerably more information can be obtained from the data than has been given in the paper.

The records obtained indicate for lines equipped with ground wires:

(a). The current waves in the stricken tower have relatively steep wave fronts and relatively short tails.

(b). Due to the steepness of the wave front, the ground impedance of driven rods and counterpoise wires is considerably higher than low-frequency resistance measurements indicate at the stricken tower.

(c). Measurements of crest currents in ground wires and towers can be used to calculate the approximate wave shapes in the stroke, in the towers, and in the ground wires.

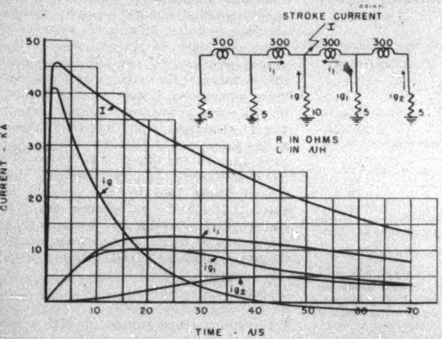


Figure 1. Current wave shapes in ground wires and towers for a 2 x 40 lightning-stroke current to a tower

These points are shown in somewhat more detail, from the results presented, as follows:

1. At towers close to the point of contact of the lightning stroke, the driven rod conducts currents of the order of three times as great as conducted by a counterpoise indicating that the impedance of (2) counterpoise wires in parallel is of the order of four times as great as that of a driven rod. (The ratio of currents in the driven rods to the currents in the counterpoise are: Table VIII, *T*-7,708—5.37; Table X, *T*-7,715—2.75; Table XIV, *T*-7,721—3.00.)

2. At towers a few spans distant from the stroke, the counterpoise may conduct as much or even more current than the driven rod (as shown in Tables VIII, towers 7,705 and 7,711; Table X, towers 7,712, 7,716, to 7,723 inclusive). As shown under paragraph *5* of this discussion, the current fronts are slow at such towers and therefore counterpoise resistance may be equal to or less than the resistance measured with low frequency. Since the counterpoise is installed at the top of a very thick layer of sand, seasonal or even day by day variations in counterpoise resistance should be great depending on the moisture content of the sand. The driven rod reaching below the sand layer should be affected to a much lesser degree. This seems to be indicated by stroke 6, Table XIV, where at towers, 7,716 to 7,719 and 7,722, 7,723, in spite of high driven rod currents, no counterpoise currents were measured.

3. At the stricken tower where the ground impedance is effective rather than ground resistance, seasonal variations are small (see paragraph 1).

4. The effective impedance of the grounding system at the stricken tower is several times the resistance measured by low-frequency means (d-c or a-c). Stroke 6, near *T*-7,721, Table XIV, caused flashover of three insulator strings at *T*-7,721, (1.5×40 negative flashover voltage of 760 kv), although the product *IR* (neglecting counterpoise resistance) was 10 ohms × 56,000 amperes which equals only 560 kv. Actually, the flashover voltage will be considerably greater on account of the coupling factor between line wires and ground wires and the short time during which the high voltage is applied as will be shown later. The actual flashover voltage, taking into account these factors, is of the order of 1,200 kv. The effective impedance for the stroke at tower 7,721 is therefore $\frac{1,200,000}{56,000}=21.4$ ohms. The ratio of counterpoise impedance to driven-rod impedance equals three on the basis of the inverse ratio of their currents. Further, the two counterpoises and the driven rod represent a parallel connection of impedances. From these two relations between the impedances, the ground-rod impedance at current crest calculates as 28.5 ohms or 2.85 times the low-frequency resistance, while the two counterpoises have a combined resistance of 85 ohms or 170 ohms each. Such high values of counterpoise impedance have been shown to exist from tests[1,2] on counterpoises and are due to steep front current waves.

5. From the data showing currents in the different grounding structures caused by the different strokes, a fair estimate of the wave shapes involved at the different towers can be made, using traveling wave theory. However, instead of repeated reflections, use is made of the fact that a transmission line after a few reflections changes from a surge impedance into an inductance. A simplified circuit such as shown in Figure 1 of this discussion results, and this can be solved with second power equations. Further, instead of voltage waves, a current wave of a desired shape is injected into the circuit representing the stroke current. This obviates the use of the unknown surge-impedance value of the lightning stroke. The resulting current waves in ground wires and towers in a section typifying towers, 7,713, 7,714, 7,715, 7,716, and 7,717, stroke 3, Table X are also shown on Figure 1, assuming a wave shape of the lightning stroke as 2×40 and equal ground resistances at all towers, as well as equal inductance per span as shown on Figure 1. This figure does not use actual resistances but shows the mechanism involved at the ground end of a lightning stroke to a ground wire.

6. It is impossible here to go into any detail about the accuracy of the method. However, Table I shows a very good check between calculations and measurements for towers 7,715, 16, 17. Towers 7,715, 14, 13 do not check as well, because tower 7,714 has a resistance of only 8.5 ohms as compared to 11 to 14 for the other towers. The ground currents in towers 7,714 and 7,716 are in inverse ratio to their resistances. In the example resistance at towers removed from the stricken tower have been assumed considerably lower than the stricken tower on account of the slow rising current fronts. To simplify calculations all resistances are made equal except at the stricken tower which is twice as great. In stroke 3, contact actually was made to the ground wire between towers 7,715 and 7,714. However, the distribution of currents is such that it must have occurred within 10 to 50 feet of tower 7715. The stroke current amplitude, therefore, is between 46,000 and 48,000 amperes with an approximate wave shape of 2×50 microseconds. Similar calculations can be made for the other strokes. It can be seen from Figure 1 that the voltage at the stricken

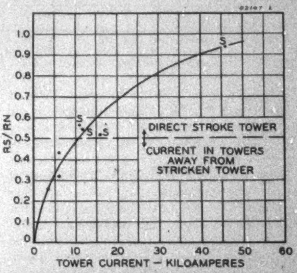


Figure 2. Ratio of surge resistance to normal resistance—R_S/R_N of towers as function of tower current amplitude

Data from Table XVI of the paper. *S* indicates stricken tower

tower ($I_g \times R$) is of very short duration (2×9.5), and therefore the sparkover of an insulator string would occur at approximately 20 per cent to 30 per cent higher voltage. This fact has been made use of under paragraph 4.

7. In Table XVI, the authors give the results of an attempt to calculate the reduction of ground resistance with increase of tower potential calculated from the product $I_t \times R_N$, where I_t is the measured tower curent and R_N the measured Megger or a-c resistance of the ground. From this, they derive an equation and the curve of Figure 2 of the paper. I wish to point out that this curve is much too optimistic, because it says, for instance, that for a ten-ohm ground resistance, a tower current of 1.2×10^6 amperes would cause only 600 kv. Likewise, a tower current of 150,000 amperes and 100 ohms ground resistance would produce a tower voltage of only 600,000 volts and therefore would not cause arc-over of an insulator string of (10) insulators, spaced $4^3/_4$ inches. This is, of course, too much to hope for and is contradicted in the paper by stroke 6, where 56,000 amperes through a ten-ohm ground resistance flashed over three insulator strings. The curve shows these high reductions for three reasons.

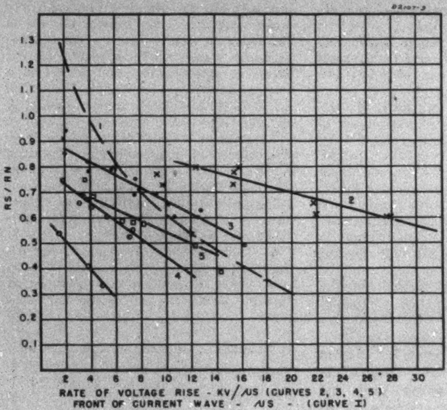


Figure 3. Effect of surge-current front on the ratio of surge resistance to normal resistance—R_S/R_N of a ground

R_S/R_N function of voltage rise

Curve	Grounds	Table	References	Current Front—Micro Seconds
2	*C, D*	II	3, I	6
3	*C, D*	IV	3, I	12
4	*H, I*	VII	3, II	18
5	*M*	VI	3, II	8–12

Curve 1—R_S/R_N as function of current front at 12.6 kv per microsecond

Table I

Ratio of Currents	Towers 7,715 7,716 7,717 Measured	Calculated From Figure 1	Towers 7,715 7,714 7,713 Measured
i_g/i_t	3.15	3.22	2.24
i_g/i_{g1}	3.25	3.97	1.96
i_g/i_{g2}	9.1	9.3	4.6
i_{g1}/i_{g2}	2.8	2.34	3.42
i_t/i_{g1}	1.03	1.23	0.88

(*a*). The assumption of a geometrical multiplication factor of 2.5 for the tower to probe potential. Such a factor should vary considerably for each tower and each stroke. Based on stroke 10, with arc-over of three insulator strings at 4,400, this factor at that tower should be of the order of 6.

(*b*). All tower resistances are treated alike and no differentiation is made between stricken towers, where the wave fronts are steep, and towers removed several spans, where wave fronts are slow as shown by Figure 1. Figure 2 shows the effect of the proximity of the stroke to a tower. Incidentally, it also shows the entirely unexpected increase in surge resistance with current amplitude.

(*c*). Combination of very high and low ground resistances. Ground resistances of the order of 1,000 ohms must not only be subject to seasonal variations to a great extent, but also should react differently to surge currents from grounds of 75 ohms or less, decreasing probably the resistance by profuse arcing across the ground surface, or down toward the water level. Test results with steep current waves are limited.[1,3] These few tests show that the steepness of the current fronts has a great effect on the impedance of a ground. Bellaschi's tests[3] which were made with fronts of 6 to 18 microseconds can be interpolated as shown on Figure 3, indicating that, even at low rates of voltage rise across the ground for short ground rods, the impedance of the rod for a 2-microsecond current front is of the order of 1.4 times the a-c resistance. This, of course, checks with the flashover of tower 7,721, stroke 6, and numerous other flashovers occurring to low-resistance structures.[4,5]

REFERENCES

1. THEORY AND TESTS OF THE COUNTERPOISE, L. V. Bewley. AIEE TRANSACTIONS, volume 53, 1934, August section, pages 1163–72.

Discussion by J. H. Hagenguth of reference 1. AIEE TRANSACTIONS, volume 54, 1935, February section, pages 220–3.

COUNTERPOISE TESTS AT TRAFFORD, C. L. Fortescue, F. D. Fielder. AIEE TRANSACTIONS, volume 53, 1934, July section, pages 1116–23.

SURGE CHARACTERISTICS OF A BURIED BARE WIRE, E. D. Sunde. AIEE TRANSACTIONS, volume 59, 1940, pages 987–91.

2. ÜBER DEN WIRKSAMEN WIDERSTAND VON ERDERN BEI STOSSBEANSPRUNCHUNG, H. Baatz. *Elektrotechnische Zeitschrift*, volume 59, number 47, pages 1263–7.

3. IMPULSE AND 60-CYCLE CHARACTERISTICS OF DRIVEN GROUNDS—I, II, P. L. Bellaschi, R. E. Armington, A. E. Snowden. AIEE TRANSACTIONS, volume 60, 1941, March section, page 123; volume 61, 1942, page 349.

4. EXPERIENCE WITH PREVENTIVE LIGHTNING PROTECTION ON TRANSMISSION LINES, S. K. Waldorf. AIEE TRANSACTIONS, volume 60, 1941, June section, page 249.

5. LIGHTNING INVESTIGATION ON A 220-KV SYSTEM—II, Edgar Bell. AIEE TRANSACTIONS, volume 55, 1936, December section, page 1307.

P. L. Bellaschi (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): The field study presented in this paper is a timely contribution on the performance of grounds. It appears to me that these findings can be compared with mutual advantage to the results of laboratory investigations recently presented to the Institute.[1]

An important characteristic of grounds is the lowering of the impulse resistance with current. This is particularly the case for grounds driven in high-resistance soil. The field data by Hemstreet, Lewis, and Foust are a further contribution on this subject. I have reference to column 9 of Table XVI of their paper. However, it is rather difficult to understand how the authors were able to draw as nice a curve as they have done in Figure 2 on the strength of the data presented alone.

Other important factors connected with the problem of grounds are the effect of the discharge circuit or lead inductance and the conditions in the earth. The lead or tower inductance becomes particularly significant when steep-front currents of high magnitude are discharged to earth. Besides the lead or tower inductance, appreciable inductance may be present also in the ground proper, as is the case for grounds driven deep into the earth, or for a counterpoise spread over a wide area. Consider, for instance, a five-ohm, 100-foot driven ground which taps its low resistance largely at the lower end of the driven rod. The total inductance of the rod is in the order of 50 microhenrys, and, accordingly, this ground would have a time constant of about $L/R = 50/5 = 10$ microseconds. A current rising to crest in a time shorter than this naturally builds up a voltage across the ground which is the sum of the two drops

$$L\frac{di}{dt} + Ri$$

For currents rising rapidly to high crest values, the initial voltage developed across the ground is largely the inductance drop, and this can reach magnitudes considerably greater than the resistance drop corresponding to crest current.

In case the resistance of the driven ground is distributed along the length of the rod, the relationship of the two drops is more complex than previously stated—only a part of the total inductance of the rod is effective. Conclusions 2 and 3 of the paper apparently have a bearing on these factors.

There are still other factors related to the problem of grounds that require study, such as the following: the distribution of electric charges in the earth preceding the lightning-stroke discharge, the variation of the soil resistivity and the soil structure, the geology, and so forth. In high-resistivity grounds, such as rocks, the capacitance of the ground conductor to the mass of the earth is not entirely a negligible factor.

REFERENCE

1. IMPULSE AND 60-CYCLE CHARACTERISTICS OF DRIVEN GROUNDS—II, P. L. Bellaschi, R. E. Armington, A. E. Snowden. AIEE TRANSACTIONS, volume 61, 1942, page 349.

J. G. Hemstreet, W. W. Lewis, C. M. Foust: J. R. Eaton has commented on soil conditions in the region of the lines investigated. Because of his close association with this work at the time the ground rods were driven and the attention he has given to soil and geology in this region, his comments are particularly pertinent. His point that ground rods in sandy soil, even though deeply driven provide a relatively high-resistance ground, should be especially noted. Low resistance is provided only when the rods penetrate the underlying clay layer.

Hamilton Treadway calls attention to the fact that this paper did not shed light on the time impedance characteristic of the ground electrodes during the lightning stroke. Of course, as he points out, our methods of measurement wherein only peak currents and voltages were recorded did not provide oscillographic results. Co-ordination of peak currents and voltages is not directly obtained from the data.

However, general experience has shown quite conclusively that the most important factors contributing to tower potential are current amplitude and footing resistance. A high initial impedance potential does not seem to be an important factor in the average circumstance of insulator flashover. In other words, the inductive potential occurring with the initial rise of current is indicated by measurement to be considerably less significant than the ohmic potential of the earth adjacent to the tower footings.

Mr. Hagenguth suggests that we could obtain considerably more information from the data than have been given in the paper. Undoubtedly this is true; however, studies of lightning on transmission lines have, in the past, more frequently been characterized by overreaching interpretations than by "too careful" ones. Our own efforts have been to stick as closely as possible to the data with simple and direct analyses. We try to measure currents and voltages in various parts of the line structure and to relate these to each other in such manner as to achieve as consistent a picture as possible. Our continued examination of structure measurements has in most cases given us a consistent picture when we consider only resistances, resistance voltage, and current summations. This method of analysis is particularly applicable to local conditions within a few towers of the lightning stroke.

With regard to seasonal changes in counterpoise resistance, considerable variation is indicated by the figures of Table II of this discussion.

At tower 7,720, which is next to 7,721, discussed by Mr. Hagenguth, the maximum variation is 420 to 220 ohms. At other towers variations are high as 6 to 1.

Mr. Hagenguth calls attention to the flashover we reported at tower 7,721 although the *IR* product was only 560 kv. Actually we are uncertain of the occurrence of this flashover. The report from the field read as follows:

> "Links taken from the *N*-14 line were magnetized without the line tripping out, so we cannot give much information on how the transmission system was affected by this particular stroke of lightning, except the insulators on tower 7,721 show indications of recent lightning flashover."

Mr. Hagenguth's interpretation of wave shapes from crest current readings and on the basis of injection of a representative wave shape is interesting but may involve too much assumption. With regard to his comment on our Table XVI and Figure 2 showing the relation between normally measured resistances, lightning-current resistances and tower potentials, he points out that we are much too optimistic because a ten-ohm resistance, and a tower current of 1.2×10^6 amperes would cause only 600 kv, or 100 ohms and 150,000 amperes would give the same voltage whereas for stroke 6, 56,000 amperes and ten ohms flashed over three insulator strings. The latter case is that of tower 7,721 referred to above where no tripout occurred, and the indication of three-phase flashover is, therefore, unlikely to have been associated with this record. With regard to the optimism of the curve of Figure 2 of the paper, our experience on this line to date does not indicate that it is overly optimistic. The method of interpretation we have included in the paper seems to us to be direct and logical. Undoubtedly additional data will permit of more exact location of the curve.

Table II. Resistance Measurements of Right-Angle Counterpoise

	Resistance—Ohms Tower				
Date	7,660	7,670	7,680	7,712	7,720
6—1937	225	350	600	540	420
7—1937	168	250	330	320	232
11—1937	155	230	310	350	250
10—1938	1,200	300	300	400	350
7—1940	175	270	350	350	400
6—1941	190	240	350	425	300
10—1941	125	170	235	275	220

Information on tripouts generally confirms our analysis. Confining our attentions to the experimental section of the *N*-14 line of six high-current disturbances, only one had an associated tripout. This was at a location where right-angle and parallel counterpoises were installed, and resistances were several hundred ohms. All five others were at locations where deep-driven rods reduced measured footing resistances to 13 ohms or less. On the *T*-20 line four tripouts were reported for the eight lightning strokes, and these corresponded in all cases with high-resistance towers not equipped with ground rods.

Mr. Waldorf has asked if the data give any indication that rods or counterpoise are the more effective in the prevention of flashover. Now the answer to this question should be based on directly comparable data on a flashover basis, but with each grounding method functioning independently; these data we do not have. However, we did assume in planning these measurements that, with both types of grounding connected, the relative amounts of pickup current would indicate relative performance, and this is what we are reporting in this paper. However, it is important that the results we are reporting be regarded for the time being as pertaining to the particular and peculiar high-resistance soil condition prevailing in our test section. Based on quite limited data, however, some indication of an answer to Mr. Waldorf's question might be derived from tripout experience on the *N*-14 line for the six strokes reported. Strokes 2, 3, 4, 5, and 6 were to sections equipped with deep-driven rods and were not accompanied by tripouts with the possible exception of stroke 6 at tower 7,721, as explained in our reply to Mr. Hagenguth. Stroke 1 to a right-angle parallel counterpoise section, however, did result in tripout which was not mentioned in the paper. No flashed insulators were reported. The rods seem to have the best of it on this basis.

P. L. Bellaschi suggests that the lightning field data presented in this paper could, with advantage, be compared to laboratory results reported in item 8 of the list of references appended to the paper. The authors appreciated this in the preparation of the paper and believe the natural and artificial surge results are in general agreement. In Mr. Bellaschi's laboratory results the ratio of "normal" to "surge" resistance was much lower than the extreme of 25 to 1 found in the case of natural lightning. However, his low ratio applies to a soil of better conductivity than the sand in the region of this experiment. As regards the importance of inductive potential along the tower and in the long ground rod, field measurement experience does not indicate this to be of major importance in the building up of flashover potential. In some of our early measurements a section of tower length some 40 feet long was bridged by a crest voltage-indicating instrument, for the purpose of measuring voltage difference along the tower. Ninety per cent of all measured voltages were below 40 kv with many cases of no voltage registration (below 10 kv) where high tower currents were recorded. Accordingly, inductive potential along the tower has not been emphasized. With regard to conclusion 3 of the paper to which Mr. Bellaschi refers, as having bearing on this matter, the time displacement between counterpoise currents and rod currents has to do with the longer current path through the deep-driven rods (some 100 feet) into the low-level conducting strata and out to remote portions of the induced earth field. Insofar as this displacement represents inductance, Mr. Bellaschi's comments are pertinent.

Abnormal Currents in Distribution Transformers Due to Lightning

Discussion and authors' closure of paper 42-109 by J. M. Bryant and M. Newman, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, August section, 1942, pages 564–8.

M. C. Westrate (The Commonwealth and Southern Corporation, Jackson, Mich.): This paper presents a very interesting approach to the problem of fuse outages on distribution transformers in the theory that surge currents saturate the core and thus allow abnormal power current to flow through the primary winding. This may be the answer to much of the trouble experienced with fuse outages during lightning storms and merits further consideration.

The point made by the authors in regard to lightning strokes producing large currents in the secondary windings of the transformers may be theoretically possible, but it is doubtful if this is a very frequent occurrence or a serious factor in the failure of distribution transformers.

D. D. MacCarthy (General Electric Company, Pittsfield, Mass.): Messrs. Bryant and Newman have analyzed the effect of long-duration surges on grounded neutral distribution circuits and concluded that these surges on the primary lines are one cause of primary fuse blowing. This is supported by Figure 3 in the paper showing that in service fuses were blown on a single-phase rural line only at locations where the primary currents were the largest. The authors state that the primary currents may become relatively large when the core of the transformer becomes saturated, and a step-by-step calculation is given for a three-kilovolt-ampere 7,200-volt transformer which is based upon the transformer design constants, the magnetization curve, and an assumed surge of 55 kv crest decaying exponentially to half value in 2,500 microseconds.

This conclusion regarding the current in the transformer primary agrees with results obtained in Pittsfield during investigation of the effects of long-duration surges made soon after the first actual oscillographic measurement[1] of long-duration lightning strokes to the Empire State Building in 1937. As a part of this study tests were made on a three-kilovolt-ampere, 6,900–115/230-volt distribution transformer of 3.38 per cent impedance. Surges were applied to one high-voltage terminal of the unexcited transformer from a 56 microfarad impulse generator charged to voltages of 20 or 50 kv; the other high-voltage terminal of the transformer was grounded. Representative oscillograms with the transformer secondary winding open-circuited are shown in Figure 1 and in Figure 2 of this discussion with that winding short-circuited. The crest values of primary current and voltage were about the same, irrespective of whether the secondary winding was open-circuited, short-circuited, or was connected to a resistance corresponding to a full load. When the secondary winding was open-circuited, or with it closed on full-load resistance, the primary current does not reach an appreciable value until the iron becomes saturated. This time to saturation, discussed by Bryant and Newman, can be seen in Figure 1 of this discussion. When the transformer secondary winding is short-circuited, there is no delay in the starting of the primary current, since the currents in the two windings give equal and opposite magnetizing forces. In this case the initial rate of current rise is governed by the leakage inductance of the transformer as might be expected.

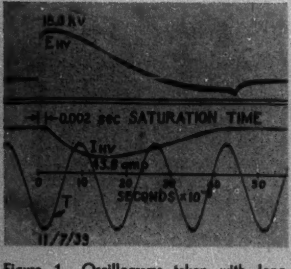


Figure 1. Oscillograms taken with long-duration impulse applied to one high-voltage terminal of three-kilovolt-ampere 6,900-volt distribution transformer with low-voltage terminals open-circuited

E_{HV}—Voltage between high-voltage terminal and ground
I_{HV}—Current through high-voltage winding
T—60-cycle timing wave

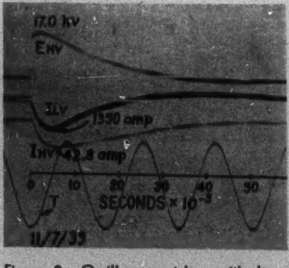


Figure 2. Oscillograms taken with long-duration impulse applied to three-kilovolt-ampere distribution transformer with the low-voltage terminals short-circuited

E_{HV}—Voltage between high-voltage terminal and ground
I_{HV}—Current through high-voltage winding
I_{LV}—Current through low-voltage winding
T—60-cycle timing wave

Other tests were made with impulse voltages as high as 40 kv applied to the transformer primary winding. In these tests fuses rated three amperes could be blown by one discharge.

Transformer inductance was calculated from the current and voltage oscillograms and is found to drop from the magnetizing inductance of 1,400 henrys to about 2.5 henrys for primary currents of 50 amperes and approach the leakage inductance of 0.9 henry with primary currents of 125 amperes. Values of primary resistance were calculated from the voltage at the crest of the current discharge and were found to be five to 30 per cent higher than the d-c resistance, presumably because the core and eddy current losses appear as a resistance.

After having studied the transformer characteristics, the effect of long-duration surges on single-phase grounded neutral circuits was investigated through the use of model lines and calculation. The results obtained from one of the conditions studied are shown in Figure 3 of this discussion in which a long-duration stroke of 500 amperes constant current was assumed to strike the phase wire at the mid-point of a 6,900-volt distribution line, 40 miles in length. It was assumed that there was one three-kilovolt-ampere distribution transformer per mile and that the line was fed from a 60-kva substation transformer. The voltage between the line conductors at the stroke location was assumed to be limited by a valve-type lightning arrester.

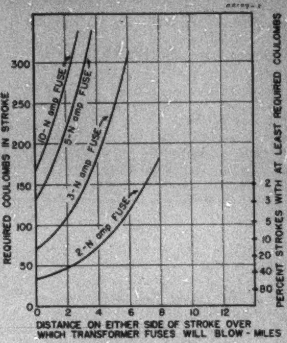


Figure 3. Coulombs required to blow fuses of three-kilovolt-ampere 6,900-volt transformers connected to single-phase grounded neutral line 40 miles in length. Stroke of 500 amperes assumed to strike phase wire at mid-point of line

Curves plotted in Figure 3 of this discussion show the stroke charge required to blow fuses of several different ratings as a function of the distance in either direction from the mid-point of the line where the stroke occurred. The right-hand scale shows the percentage of strokes expected to have charges at least as great as read from the left-hand scale based upon lightning data[1] from the Empire State Building. The distribution transformers and fuses were assumed to be the same for each location. There are a number of factors in service which will affect the locations where transformer fuse blowing will occur. Among these are differences in the characteristics of the fuses, distribution transformers, variations in resistance of the grounds along the line, and the possibility that flashover may occur between the line conductors due to the high current peak of the lightning stroke which generally precedes any long-duration low current component that may occur. However, these curves lead to the conclusion that fuse blowing on such a line will be restricted to the vicinity of the stroke and that the number of fuses blown decreases very rapidly as the fuse rating is increased. The impedance of the distribution line and the density of connected transformer kilovolt-amperes greatly influence the number of fuses that can be blown. The high impedance and light load of the line considered in Figure 3 of this discussion are favorable to fuse blowing by long-duration surges. Yet with five-ampere fuses only three per cent of the strokes that occurred to the Empire State Building have sufficient charge to blow the fuse of the transformer at the stroke location.

As Bryant and Newman state, more data are needed to establish the frequency with which long-duration discharges blow transformer fuses in service. This frequency must, of course, be established in order to judge the permissible cost of any solution proposed. It is difficult to determine the portion of fuse blowing that is caused by long-duration discharges. Probably the most complete compilation of operating data on distribution transformer lightning protection was prepared by L. G. Smith[2] based on reports from approximately 40 utilities covering urban and rural territories for a period of three years; with conventional arresters and solid interconnection, the rate of fuse blowing was 1.85 per cent for urban and rural territory combined in Table X, and 6.26 per cent for rural territory only in Table XI. Some undetermined part of this fuse blowing was probably caused by lightning currents flowing in the transformer primary windings.

In looking for means of preventing impulse currents from blowing primary fuses, the author of this discussion and his associates developed a fuse with a small series inductance and a protective gap shunting the current responsive element of the fuse link and inductance. A number of years ago, a trial lot of these fuses was installed for experimental purposes on the line side of arresters protecting distribution transformers in order to gain service experience. The number of these special fuses blown by the relatively high impulse currents discharged by the arresters was much less than for standard fuses, see Table VI of reference 3. However, it was concluded that the combination of series inductance and shunting gap is relatively ineffective in preventing such fuse blowing as may occur because of lightning currents building up in the transformer primary winding, because, with any practical fuse design, the shunting gap will not spark over on account of the slow rate of current rise. Furthermore, should the shunting gap spark over because of a pulse of current through the transformer capacity, it will seal and long-duration current that may subsequently build up through the transformer primary winding will flow through the fuse element.

Only strokes to the primary lines are mentioned by Bryant and Newman. However, the effect of strokes to other parts of the circuit was investigated by the author of this discussion, and it was concluded that primary fuses can be blown by long-duration strokes to the secondary wires or to the earth near the ground connections at the transformer pole or on the secondary circuit. Unquestionably some portion of fuse blowing is due to strokes that do not reach the transformer over the primary circuit.

REFERENCES

1. LIGHTNING TO THE EMPIRE STATE BUILDING, **K. B. McEachron.** *Journal* Franklin Institute, volume 227, 1939, February section, pages 149–217.

2. DISTRIBUTION TRANSFORMER LIGHTNING-PROTECTION PRACTICE—II, **L. G. Smith.** AIEE TRANSACTIONS, volume 57, 1938, April section, pages 196–216.

3. MEASURED LIGHTNING CURRENTS THROUGH DISTRIBUTION ARRESTERS ON 4,800-VOLT RURAL CIRCUITS, **H. R. Wilbur, W. A. McMorris.** *General Electric Review*, volume 44, 1941, March section, pages 159–65.

J. H. Hagenguth, H. C. Stewart (General Electric Company, Pittsfield, Mass.): We wish to discuss the first part of the paper describing the failure and deformation of the low-voltage winding of a distribution transformer. A few such cases, as described in the paper, have come to our attention. The number is so insignificant compared with transformers in service that in all probability it would not be economically justified to provide protection.

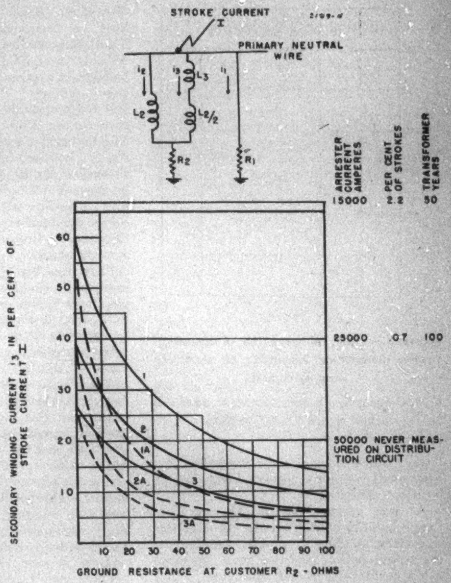


Figure 4. Abnormal secondary - winding current as function of stroke current

Service pole remote from other poles. Solid horizontal lines are 10,000 amperes secondary - winding current levels for respective lightning-arrester currents

Service pole ground $R_1=30$ ohms, curves 1, 2, 3
$R_1=10$ ohms, curves 1a, 2a, 3a
Customer's ground $R_2=$ abscissa
Secondary - winding inductance $L=60$ microhenrys

Secondary-neutral lead inductance	$L_2=$	320	55.4	27.7
Secondary-line wire inductance	$L_2/2=$	160	27.7	13.8
Length of secondary leads		600	100	50
Curve		1, 1a	2, 2a	3, 3a

$I=0\times00$ wave

The authors point out that, "such conditions, to a greater or lesser extent, must occur in almost every case of a direct stroke to line near a transformer installation."

It is, of course, immaterial whether the stroke contacts the neutral wire directly or by way of the high-voltage line wire and the arrester. Some brief calculations have been made to show that the possibility of the occurrence of such phenomena should be extremely rare. Figure 4 of this discussion shows the circuit used as basis of the calculation, which represents the condition of a transformer installed on a line where the nearest transformer is at least five miles removed. This is, of course, purely hypothetical but represents the most severe condition possible, because practically all the current in a stroke has to be conducted to ground at that location. The secondary neutral lead is represented by an inductance based on 0.55 microhenry per foot. The inductance of the two secondary-line leads are assumed to be in parallel and therefore half the value of the neutral wire. (Since all three wires carry current in the same direction and are closely spaced, these inductance values are not mathematically exact, but they are of the right order.) The use of the inductance is justified, because after only a few reflections, the surge impedance of the wires changes into an inductance.

The solid curves of Figure 4 show the current amplitude through the secondary winding in per cent of the total current at the neutral wire, when the service pole ground is 30 ohms, as function of the resistance of customer's ground. All ground resistances are effective resistances. The dashed lines show the currents for ten-ohm pole ground. Stroke current wave shape used for calculation is 0×00.

The authors assume that damage to the transformer shown in their Figure 1 was probably caused by a current of 20,000 amperes in each section. If we assume even one quarter of that value through one section or 10,000 amperes through both sections, the three horizontal lines indicate the levels at which 10,000 amperes would flow through the secondary for different stroke currents as indicated at the first right-hand scale.

McEachron and McMorris[1] have shown that the currents through distribution arresters are of relatively low value, the highest measured current from 1,608 records was 25,000 amperes (six records or 0.003 of the records indicated values in excess of 25,000 amperes). The percentage and expectancy figures given in their Figures 1 and 2 are shown on the right-hand scale of Figure 4 of this discussion. Curves 1, 1*A*, 2, 2*A*, and 3, 3*A* are drawn for 600-, 100-, and 50-foot long secondary lines between transformer and customer, respectively.

Figure 4 shows clearly that the chances for even 10,000 amperes through a distri-

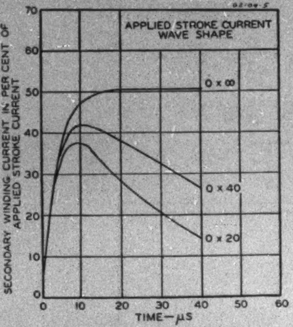


Figure 5. Effect of wave tail of lightning-arrester currents on amplitude of secondary winding currents

R_1 = 30 ohms, R_2 = 5 ohms, 600-foot extension (See Figure 1 for diagram)

bution transformer secondary winding are very small even for the isolated transformer location calculated.

As was pointed out by the authors, the probability of high currents through the secondary winding is rapidly reduced as the service pole ground resistance decreases below customer's ground resistance.

However, this is a solution that should hardly be recommended because of the associated hazards. The resistance at the customer should be kept to as low a value as possible. Figure 4 of this discussion shows that even with 0 ohms resistance at the customer and 10 ohms or 30 ohms at the pole, high currents through the secondary winding are improbable.

The circuit shown in Figure 4 neglects the resistances of the secondary leads and windings. Since the inductances and resistances are relatively small, current flow quickly is distributed according to resistances rather than inductances. The equations for circuit of Figure 4 which are of the form $A(1-\epsilon^{-nt})$ were used because they readily can be evaluated for crest current. The effect of resistances is similar to that of the inductances, and therefore a current time curve is practically the same for both conditions.

There is a very good reason to believe that current peaks to distribution systems are of relatively short duration, perhaps not longer than 40 microseconds, and consequently the duration of the secondary winding currents would be reduced correspondingly to shorter durations. The effect of the length of the current wave is most pronounced for conditions of highest currents through the secondary winding. Figure 5 of this discussion shows the current wave shape through the secondary winding for rectangular tail, 40-microsecond tail and 20-microsecond tail of the current wave through R_1 and R_2 of Figure 4. The marked reduction in current crest is evident.

The wave tail of the current through R_1 and R_2 is determined to a considerable extent by the presence of other pole and customer grounds near the location struck by lightning. The effect of two adjacent grounds on either side, 1,000 feet and 5,000 feet distant, is shown on Figure 6 of this discussion. Again the primary neutral lines are represented by inductance and infinite long stroke current wave is assumed. The current $i_1+i_2+i_3$ is rapidly reduced to half value, even for 5,000-foot distance between grounds. Therefore, the duration and the amplitude of secondary abnormal currents are definitely limited. (This calculation neglects the inductance between stricken pole and customer. The ground resistance at the two far poles are assumed to be normally 30 ohms reduced to half to take account of resistance reduction[2] caused by impulse currents on account of the slow current fronts and customer's ground at their locations.)

From this brief study, it is shown that secondary winding currents of sufficient amplitude to damage the secondary winding in the way shown in the paper are hardly to be expected if the stroke contacts the primary neutral wire, although the possibility exists.

There is, however, a good possibility that damage of this type is caused from strokes contacting the electrical wiring at the customer's premises and due to high ground-resistance current flows from customer's wiring to the service pole. The higher the customer's ground resistance, the greater, of course, is the possibility of damage to the secondary winding, because there are no other additional connections which may drain the bulk of the lightning stroke current.

Three cases are known to us where lightning entered a distribution system through the secondary circuit without even then causing this particular damage. One is described in another paper.[3] In this case, the energy of the stroke was carried to ground through three different grounds. Another case occurred in the city where the customer was grounded to the water system and a third at a Civilian Conservation Corps camp in wooded country.

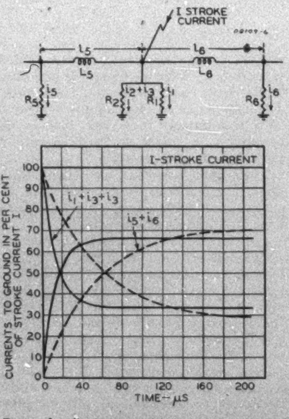


Figure 6. Reduction of length of current tail at point of stroke contact because of grounds at adjacent poles

Primary neutral wire inductance
$L_5 = L_6$ = 554, 2,770 microhenrys
Distance $l_5 = l_6$ between service poles
1,000, 5,000 feet
Ground resistance $R_5 = R_6$ 15, 15 ohms
Ground resistance $R_1 = R_2$ 30, 30 ohms

The greatest exposure to such damage should be expected on rural lines where the primary neutral wire is grounded at perhaps only a mile interval. The principal protection as shown by the curves of Figure 4 would be low service pole ground resistance and short secondary leads with customer's ground at a minimum economical value.

REFERENCES

1. DISCHARGE CURRENTS IN DISTRIBUTION ARRESTERS—I, II, **K. B. McEachron, W. A. McMorris.** AIEE TRANSACTIONS, volume 54, 1935, December section, pages 1395–9; volume 57, 1938, June section, pages 307–14.

2. IMPULSE AND SIXTY-CYCLE CHARACTERISTICS OF DRIVEN GROUNDS—II, **P. L. Bellaschi.** AIEE TRANSACTIONS, volume 61, 1942, pages 349–63.

3. EFFECT OF LIGHTNING ON THIN METAL SURFACES, **K. B. McEachron, J. H. Hagenguth.** AIEE TRANSACTIONS, volume 61, 1942, August section, pages 559–64.

E. E. Piepho (The Detroit Edison Company, Detroit, Mich.): During every lightning storm there are a few mysterious cases of blown primary fuses on distribution transformers, with no evidence of flashover or other failure on the transformer side of the fuse. Bryant and Newman's proposition that such events may be caused by long-duration lightning strokes which establish sufficient surge current through the primary winding to blow the transformer fuses, appears a likely possibility. This situation has not been serious in the past, because we have generally avoided the use of low-current fuses, the five-ampere size usually being the smallest. Admittedly, this minimum size does not protect the smaller distribution transformers from secondary faults or even winding short circuits, but it serves the larger purpose of isolating a transformer in serious trouble from the rest of the circuit. Anyone undertaking to use low-current fuses, rated to provide complete transformer protection, may expect an appreciable increase in fuse blowings from electrical storms.

Two years ago, when Bergvall and Beck presented their paper on "Lightning Protection of Distribution Systems," the writer suggested that their treatment of the subject indicated that we might have to revise some of our thinking regarding protection of electric equipment and distribution circuits, in accordance with the new conception of long-duration lightning discharges. A wholesale blowing of customer branch fuses in one locality during a lightning storm was cited at that time, and it was indicated that long-duration lightning discharges into secondary wiring by way of the transformer connections may have been the cause. Bryant and Newman's paper offers good proof that this may occur.

Recently, we experienced an interesting case of lightning damage to the operating mechanism of a street-lighting time switch. From tests we have made we know the insulation level of the low-voltage operating mechanism is approximately 4,000 volts, which is rather low considering the degree of lightning exposure. We devised adequate lightning-arrester protection for this insulation, but a severe electric storm recently demonstrated an unlooked-for weakness.

Paralleling the low-voltage arrester there was a sneak circuit through the high inductances of the series field and armature of the time switch motor. We expected little or no surge current through this branch because of its high inductance; nevertheless, the motor field and armature windings were burned open in one switch. The insulation of these windings to ground was tested and found unimpaired. Our belief is that a long-duration discharge through the arrester could have established enough current through these low-current windings to burn them open.

Edward Beck (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Bryant and Newman discuss an aspect of lightning on systems which is of particular interest on the lighter rural distribution circuits, since it contributes to outages where the fusing is light. One phase of this general subject has been discussed in the Bergvall and Beck paper mentioned by the authors, the phase dealing with the effect on arrester duty, which is relieved by the currents that flow through the transformers. The Bryant and Newman paper deals with the transformer currents and their effects on the transformer and the fusing. Considering the capacities of the smaller standard fuses, such as the two-ampere links mentioned by the author, for carrying current without damage, it is not surprising that the surge currents blow them occasionally. The danger of primary fuse blowing is diminished with increasing rating. This is almost axiomatic, and it has been shown by statistical data.

It is practically impossible to fuse small transformers on the primary against overload or secondary short circuits and maintain a satisfactory degree of reliability and service continuity. Attempts to accomplish this with conventional fuse links has led to two types of fusing. The first of these is to fuse with small links in an effort to prevent transformer burnouts from overload. This type of fusing leads to a large number of outages caused by blowing of the links from secondary fault, lightning currents, and mechanical damage. The second type of fusing is to select links based upon secondary short-circuit currents. The larger links resulting from this type of fusing reduce the number of operations to some extent from the causes mentioned above but at the expense of possible burning out of the transformer from overload and short circuits remote from the transformer. The economics appear to favor the second type, and there is a definite trend towards this type of fusing by operating companies. There is another type of overcurrent protective system which provides both overload and short-circuit protection to the transformer and at the same time eliminates unnecessary interruptions of service. This method employs a thermally controlled secondary breaker mounted inside the transformer. By having the breaker operation based upon transformer winding temperature, protection of the transformer is afforded for all types of overcurrent conditions, and at the same time it is possible to take advantage of the inverse time temperature characteristics of the transformer to adjust the breaker so as to eliminate most of the interruptions in service encountered through the use of small fuses. There is now a large amount of service experience available with these types of overcurrent protection, and it would be of interest and value both to operators and to manufacturers to have comparative operating data published.

J. M. Bryant and **M. Newman:** The writers are pleased that the paper has led to considerable discussion. D. D. MacCarthy's comments and oscillograms on long-duration currents and fuse failures are interesting and in complete agreement with our work. As a matter of fact we have recorded almost identical oscillograms to those given by MacCarthy, which were, however, not included in this paper as they fitted in more directly in a series of papers on "Fuse Failures Due to Lightning," published by the engineering experiment station, of the University of Minnesota. We were interested to learn that MacCarthy and his associates have had some favorable field experience with a series inductance and shunt gap arrangement of protection of fuses against surge outages, as we had investigated such a scheme proposed a number of years ago by R. R. Pittman of the Arkansas Power and Light Company. It was established on first consideration that such a scheme by itself functions primarily to by-pass around the fuse the initial current surges due to the transformer capacitance under conditions of rapid rate of rise of surge voltage. The long-duration currents penetrating the transformer windings were eliminated as a factor in fuse blowing in several different ways, such as some special saturation by-pass schemes, and proper design of heat storage capacity of the links themselves. The theoretical background on fuse failures because of lightning is presented in the experimental station bulletins referred to above. Field experience is being collected on the simplest combinations of the various methods of surgeproof fuse design.

The use of secondary breakers, as brought up by Edward Beck, is a perfectly good way of preventing lightning-fuse failures, in that it side-steps the problem by making the primary fuse so large as not to respond to surges, and thus to remove the transformer only if it is already damaged, while depending on breakers on the secondary side for overload protection. We have been carrying on some researches to help solve the surge problem in relation to fuses from the point of view that as fuses are being used in any case, to isolate the transformer from the line after burnout, it would perhaps be more economical if they also protected the transformer.

The discussion by J. H. Hagenguth and H. C. Stewart gives a very interesting approximate theoretical estimate of the extent to which other cases of damage of secondary windings may occur. We do not, however, quite agree with attributing a greater possibility of such cases from strokes on the secondary side, since under such circumstances the paths to ground from the transformer case neutral grounding point is the same anyhow, and there is a much greater probability that the stroke would short-circuit the low-voltage leads together so that the transformer windings would be bypassed. While on the subject of strokes on the secondary side, it is of interest to note that it may be possible for surge currents to build up in the secondary winding after the manner of the abnormal currents in the primary winding discussed in the paper. Such build-up would be generally accompanied by sufficient voltage to induce an arrester flashover and primary fuse failure; several such cases have been observed. The case of secondary-winding damage in the paper is, however, definitely due to current entering the winding from the neutral connection, as shown by the opposing directions of twist of the winding sections.

The comment by M. C. Westrate on the desirability of further investigation of effect of abnormal power current on fuse failures under surge transformer saturation conditions seems to us to be well worth emphasizing. The discussion by E. E. Piepho brings out several interesting cases of lightning failures that might be readily accounted for, as he points out, by such analysis as presented in the paper. The writers in conclusion are pleased to note particularly that the discussion has been constructive in leading to further extension of these investigations in the field.

Induced Voltages on Transmission Lines

Discussion and authors' closure of paper 42-103 by C. F. Wagner and G. D. McCann, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, pages 916–30.

W. W. Lewis (General Electric Company, Schenectady, N. Y.): The authors have prepared an excellent and exhaustive paper on the subject. I wish to discuss a few points:

Figure 28 of the paper shows distribution curves of crest current in lightning strokes as determined by various investigators. In the text it is stated that the maximum current which has been reliably measured is 160,000 amperes. This refers to a Fulchronograph measurement of a stroke to a stack at Anaconda, Mont., and in reference 10 of the paper it is stated that this represents the highest known magnitude in which the total current was measured at one point, as contrasted with measurements obtained by summing up currents in parallel paths.

We have shown in several papers[1,2] reasonably good agreement between total current and the summation of individual currents in parallel paths, and, considering, the wide tolerance necessary in this class of measurement, we believe such estimates to be as reliable as those made by any other method.

In 1934 on the Glenlyn–Roanoke 138-kv line of the Appalachian Electric Power Company, measurements were made by magnetic links of a stroke with an estimated total current of 218,000 amperes negative polarity. This current was the summation of the currents in all the towers affected by the stroke, seven towers in all.[3] The maximum current measured in any one tower is 132,000 amperes obtained on the Philadelphia–Delaware River 66-kv line of the Philadelphia Electric Company in 1935. This was also the total current in the stroke.

Another stroke in the same year on this line totalled 156,000 amperes in two towers. Magnetic link measurements have indicated 146,000 amperes in the broadcasting tower of station *WABC* and 156,000 amperes in the tower of the Empire State Building.

In Figure 28 of the paper a curve by Waldorf is shown, giving distribution of crest current in lightning strokes, and reference 15 in the paper is given, presumably for the origin of this curve. Waldorf's original curve (Figure 1 of reference 15 of the paper) shows the distribution of 1,201 current measurements in *towers* and does not give total stroke current. We believe therefore that this curve should not be included in Figure 28, which deals with total stroke currents.

The authors state: "There are very little field data on lightning surge voltages for which the induced voltages can be segregated from those produced by direct strokes." In 1929 at the Wallenpaupack laboratory, 95 cathode-ray oscillograms were obtained of voltage surges on the line. Of these 50 measured under 100 kv, 30 measured between 100 and 300 kv, and 15 measured above 300 kv. Data on the 45 oscillograms above 100 kv are given in reference 4 of this discussion. In the same year at Newcomerstown, ten oscillograms were obtained ranging from 55 to 245 kv. Data on these are given in reference 5 of this discussion. In 1930 at Cherry Valley oscillograms of 23 surges were recorded. Data on these are given in reference 6 of this discussion.

It is very reasonable to assume that the low-voltage positive polarity surges are induced. Peek has classified the 1929 surges as coincident with trip out and not coincident with trip out and has assumed that the latter were induced.[7] It would seem that there is a wealth of untapped information in these oscillograms.

The curves of the paper indicate an increasing induced voltage with decreasing distance between the conductor and the projection of the stroke. Suppose the stroke hit the overhead ground wire, perhaps ten feet or so away from the conductor. How would this work out as far as the induced stroke is concerned?

References

1. Lightning Investigation on Transmission Lines—VII, **W. W. Lewis, C. M. Foust.** AIEE Transactions, volume 59, 1940, April section, page 227.

2. Lightning Investigation on 220-Kv System—II, **Edgar Bell.** AIEE Transactions, volume 55, 1936, December section, pages 1306–13.

3. Lightning Currents in 132-Kv Lines, **Philip Sporn, I. W. Gross.** AIEE Transactions, volume 56, 1937, February section, pages 245–52, 259–60.

4. Lightning Investigation on 220-Kv System of the Pennsylvania Power and Light Company (1928 and 1929), **N. N. Smeloff, A. L. Price.** AIEE Transactions, volume 49, 1930, pages 895–904.

5. Lightning Investigation on the Ohio Power Company's 132-Kv System, **Philip Sporn, W. L. Lloyd, Jr.** AIEE Transactions, volume 49, 1930, pages 905–16.

6. Summary of Five Years' Work in the Study of Lightning Effects on Transmission Lines, **W. W. Lewis.** International Conference on Large High-Voltage Systems (CIGRE), 1931.

7. Lightning, **F. W. Peek, Jr.** AIEE Transactions, volume 50, 1931, pages 1077–89.

E. R. Whitehead (Duquesne Light Company, Pittsburgh, Pa.): During the first few years of field investigations of lightning on transmission lines with the cathode-ray oscillograph, it was my pleasant duty patiently to await the occurrence of induced voltages caused by the release of bound charges on the line conductor. Despite the fact that many strokes were observed to strike the earth quite close to the line, very few oscillograms were obtained, and these were of low magnitude.

Fortescue and Bewley showed that the principal reason for the low induced voltages lies in the current-time characteristics of lightning, and both assumed the charge concentrated at a point in the cloud. Wagner and McCann have applied their knowledge of the lightning-stroke mechanism in an exceptionally lucid manner to show how the actual distribution and neutralization of charges may be utilized to calculate induced voltages much more accurately. I believe their contribution is of great value.

If I interpret the curves of Figures 3 and 11 of the paper correctly, it appears that any stroke which is close enough to induce a voltage of importance on lines at 66 kv and above is very likely to strike the line itself. Now this fact leads to a conclusion not mentioned by the authors and one which I believe may be of importance in practical application.

Positive voltages of the order of 1,000 kv for a 100-kiloampere stroke to a tower would correspondingly increase the potential difference across the insulator string, since the tower would be depressed in potential by the amount of impedance drop in the tower and tower grounding system. The time of voltage crest is such that these voltage-difference components would be additive; hence the net effect is roughly equivalent to that obtained by conventional "direct-stroke" theory if a fictitious resistance of ten ohms is added to that of the tower footing.

The inclusion of this effect may be significant if the tower-footing resistances lie in the range 0 to 50 ohms, and close comparative performance calculations must be made.

Despite certain analytical difficulties, I believe the method presented by Wagner and McCann can be extended to the case of a stroke to the tower or ground wire. Preliminary studies I have made using the same stroke mechanism and L. V. Bewley's method of analysis indicate that such an extension might be the missing link which would give a satisfying aspect of completeness to our theory of lightning effects on transmission lines and dispose of the terms "direct-stroke" theory and "induced-surge" theory.

I should like to call the authors' attention to an apparent discrepancy in the data given by the curves. Most of the curves give a voltage crest of about 1,100 kv for a 50-foot conductor, and a 100-kiloampere stroke at a distance of 150 feet from the line. Figure 29, however, gives 1,100 kv for the same stroke 100 feet from a line of the same height.

J. H. Hagenguth (General Electric Company, Pittsfield, Mass.): The authors have made an excellent study in calculating the induced voltages on transmission lines using the mechanism of the lightning discharge as known from the photographic studies by Schonland (references 4–8 of the paper) and McEachron (reference 9 of the paper) and the oscillographic studies of McEachron and others as the basis of their calculations.

They find that, in general, the gradients are of the order of 100 kv per foot or less, so that basically the calculations of Bewley (references 2 and 17 of the paper) can still be used to advantage in calculations as to the induced voltages. The authors find, however, that the gradients directly at the point struck reach tremendously high values. In Figure 6 of the paper the gradient approaches infinity at $d=0$ as the stroke approaches the ground. Such high gradients, if they would actually exist, presumably should cause flashover every time a tower of a transmission is struck, which, of course, is not the case in practice.

Actually, the induced voltages appear to be considerably below the values calculated. Fortescue (reference 1 of the paper) showed that with a stroke 250 feet from a transmission line, 1,600 feet distant from a cathode-ray oscillograph connected to that line, no voltages could be measured at the oscillograph station. The photograph of this occurrence does not indicate any streamers from the line or from the tower, indicating that the gradients must have been less than 165 kv per foot, which is the critical breakdown gradient in the nonuniform field in air at large spacings (30 feet).

The theory presented appears, therefore, at variance with observations. I question that the calculations of equations 8 and 11 of the appendix are correct when considering a point directly underneath or close to the lightning channel. These calculations are based on the assumption that each charged particle in the uniformly charged channel produced a gradient equal to a point charge at that point. By integrating over the whole channel, the combined gradient is found. In the field produced by a charged vertical line, the effect of the charges distant from the ground on the gradient directly below or close to the path is negligible, and only the charges at the lower end of the channel should contribute materially to the gradients, and therefore the gradients should be considerably lower than calculated.

It appears to me that further point to be considered is the availability of charges on line conductors. These charges can accumulate by leakage over the insulators which is a longtime process associated with the charges accumulating in the cloud. These charges are distributed along the line wire and bound by all the charges in the cloud distributed over the line. While the total bound charge thus may be of considerable magnitude, these charges are not immediately available on the line near the lightning channel. The charges bound in the immediate vicinity of the channel are small, and the charges which can be brought from the distance are limited for two reasons:

1. It takes time to move these charges along the wire.

2. Some relatively short distance away from the channel the effect of the cloud field will be greater than the effect of the field produced by the return stroke, and the charges at those distances will remain bound.

From these considerations, it would appear that high voltages as shown in Figure 12 could not exist on line wires. On ground wires, the voltages at mid-span might be quite high for very short times, because charges are more readily available. However, even for this condition, flashover can hardly be expected. As shown by practical experience, even direct strokes of considerable amplitude contacting the ground wire at mid-span hardly ever cause flashover at mid-span.

R. H. Golde (The British Electrical and Allied Industries Research Association, London, England): The authors are to be congratulated on having attacked afresh the old problem of the calculation of induced voltages which has so far defied all attempts at a correct solution. I have recently made similar calculations using the same fundamental considerations with only a few modifications which prove, however, as will be shown elsewhere, essential for the numerical solution of the problem.

Before discussing the above modifications it may be pertinent to comment on the apparent agreement between the author's calculations and observed values as presented in Figure 33 of the paper. All the calculations are based on the assumption that the highest induced voltages are due to strokes to earth at a distance of three times the height of the line above ground. This, surely, overlooks the fact that much higher induced voltages must be released in the case of a direct stroke to a transmission tower or earth wire without flashover. As these cases must influence decisively the frequency-distribution curves of induced voltages, the authors appear to have started from wrong assumptions, and the agreement obtained in Figure 33 must be regarded as only fortuitous.

If it be conceded that the highest induced voltages occur in cases of direct strokes to earthed parts of the transmission system, it follows that the horizontal distance between the lightning channel and the nearest line wire is very small. This case leads, however, to excessively high-voltage values by the authors' methods, as can readily be seen if the curves plotted in Figures 12 or 29 are extrapolated to very short distances between the lightning channel and the line. The reason for this breakdown of the method adopted by the authors for the important case of short distances lies in their assumption that the induced voltages are determined only by the charge distribution along the leader channel and by the velocity of the return stroke. This results not only in voltages of several million volts but also in substantially infinitely steep wave fronts of the induced surges. It may be mentioned in passing that, on the authors' assumptions, the wave shape of the lightning current would also have an infinitely steep front with an exponential tail and would not give the normal double-exponential shape as claimed in the introductory section.

The above difficulties are overcome by recognizing that the neutralization of the charges deposited along the leader channel cannot take place instantaneously.[1,2] The authors attack this conception by the calculation given in their Appendix II but, omitting all further criticism of the method adopted, it should suffice to point out that the tip of the advancing return stroke cannot be maintained at zero potential. That part of the channel which is just above the tip of the return stroke is still in electric contact with the cloud and therefore, omitting the voltage drop along the leader channel, is practically at the cloud potential of several million volts. It is physically inconceivable that this potential falls to zero "instantaneously" on contact with the tip of the return stroke, and the transition will actually occur in the course of the few microseconds postulated for the time required for lateral neutralization of the channel charges.

Calculations made with this new conception show that the charge distribution along the lightning channel is not immaterial, as suggested by the authors. It thus follows that the voltages induced in overhead wires are determined by three factors, that is, the distribution of charge along the leader channel, the rate of neutralization of these charges, and the velocity of the return stroke. It may also be added that it seemed advisable to use an exponentially decreasing charge distribution along the channel and a similarly decreasing velocity of the return stroke. If the functions which are suggested for these parameters in the paper quoted above are accepted, the resulting induced surges in cases of strokes to earthed parts of the transmission system are of the amplitude and wave shape to be expected. A full description of the procedure adopted and of the results obtained will be published later.

In addition to the above fundamental observations on the method of calculation, a few specific points may be briefly discussed. Thus the charge deposited along the leader channel is calculated by the authors by assuming a constant current of 100,000 amperes flowing for 65.6 microseconds, that is, the time for the return stroke to reach the cloud discharge center. This assumption can hardly be regarded as valid, having regard to the wave shape of the average lightning current. It follows that the numerical value adopted for the charge along the channel is too large. The authors state, furthermore, that, at the instant when the leader stroke makes contact with the ground, the charge left in the cloud center has decreased to zero. Although this charge does not influence the occurrence of induced surges, it may be mentioned that actually an appreciable amount of charge must be left in the cloud, as shown in the paper quoted above, and also by the low current discharge flowing, even on the authors' assumption, for a considerable time after the return stroke has reached the cloud (see Figure 2 of the paper).

In this connection it was interesting to note that the authors incorporate partly in their Figure 1 the mechanism of successive strokes, as outlined in the paper quoted, which differs from Schonland's originally suggested picture. In particular, Figure 1d accepts the idea that the conducting channel which connects the exhausted charge center in the cloud with earth will become positively charged under the influence of the surrounding negative cloud charges. From this it is difficult to see how the authors account for the interruption of the positive current flow into the channel, as shown in Figure 1e. Though the interesting question of the continuous current flow throughout a multiple lightning stroke has been discussed elsewhere[3], a reply with respect to Figure 1d would be appreciated.

In Table I ranges of return-stroke velocities and their mean values are given for three current ranges. It would be interesting to know on what information these values are based, since I am unaware of any measurements of return-stroke velocities with simultaneous determination of the stroke current.

Finally, one small point in nomenclature may be cleared up. In discussing the question of lateral charge distribution the authors refer to the bright portions of the stepped leader which were observed by Schonland and add that these were called "dart leaders" in the paper mentioned above. Actually these short bright steps were called "darts," whereas the term "dart leader" was used by Schonland to distinguish the leader stroke of successive strokes in a multiple stroke from the "stepped leader," which normally precedes the first stroke to ground.

REFERENCES

1. C. E. R. Bruce, R. H. Golde. *Journal* of Institution of Electrical Engineers (London, England), volume 88, part II, December 1941, page 487.
2. SURGE PHENOMENA. Electrical Research Association, 1941. Chapter 1.
3. *Journal* of the Institution of Electrical Engineers (London, England), volume 89, part II, December 1942, page 646.

C. F. Wagner and **G. D. McCann:** The most accurate field measurements of direct-stroke currents are those for which the total current passes through a single point, such as a mast. The currents in excess of 160,000 amperes, which Mr. Lewis mentions in his discussion, were not measured in this manner but were the result of summing the individual currents in parallel paths. Several cases for which checks could be made between actual current and the summation in parallel paths showed considerable discrepancy in the two results. This point is discussed in more detail in reference 19.

The curve in Figure 28, labeled as Waldorf's, was computed from the original data supplied by Mr. Waldorf and is plotted for the crest magnitude of the total lightning-stroke current and not for tower currents. A comparison between this curve and the curve of Figure 1, reference 15, to which Mr. Lewis refers, indicates that they are not the same.

Lewis and Whitehead have raised the question of the voltage induced on phase conductors by strokes to the ground wire. This case is not considered in the paper. It requires more detailed knowledge of stroke mechanism, especially near the ground terminus, than for strokes to open ground. In regard to the influence of induction upon the voltage across the insulator string, we are not quite convinced whether its effect can be represented by a resistance, and, if so, whether that resistance is ten ohms. Certainly if this effect can be represented by a resistance, it would be a very welcome simplification of the analysis.

The apparent discrepancy between Figures 11 and 29, referred to by Mr. Whitehead, arises from the difference in assumptions for the two curves. The voltages in Figure 11 were calculated for a crest stroke current of 100,000 amperes and a return streamer velocity of 100 feet per microsecond, whereas for the voltage curves of Figure 29 the variation of return streamer velocity with stroke current has been taken into account.

There is little in Mr. Hagenguth's discussion with which the authors can agree. His reference to Fortescue's experience, reference 1, is not inconsistent with the results of the paper. As shown in Figure 29, for a line 100 feet high a stroke 250 feet from the line could have a current crest of 15,000 amperes without inducing more than 200,000 volts on a phase conductor, assuming there were no ground wires present. The coupling effect of the ground wire reduces the induced voltage still more.

Mr. Hagenguth's criticism of equations 8 and 11 as being inaccurate is incorrect. These equations are a rigorous solution for the assumptions made. They correctly represent the effective charge distribution along the stroke channel at any point along the surface of the earth.

The method of calculation rigorously accounts for the presence of the computed voltages as required for the assumed distribution of charge and current in the stroke channel. The method also includes rigorously the effects of bound charges which seem to bother Mr. Hagenguth.

The authors wish to thank Mr. Golde for his interest in the paper and for the detailed comments which he has given. It is gratifying that two widely separated investigators have come to substantially the same conclusions regarding the general nature of the phenomena. Most of the differences between the points of view regarding the assumptions that the authors use arise from Mr. Golde's application of them to strokes to earthed parts of the transmission system. The limitations of the assumptions used were recognized by the authors. Induced voltages produced by strokes to ground wires and towers, and the preliminary induced voltage preceding a direct stroke were specifically eliminated from consideration. The basis for this action lay in the lack of sufficient fundamental data concerning the nature of rates of rise of currents and the character of upward streamers, factors which have a predominant effect in determining the nature of these voltages.

Mr. Golde is under the impression that the agreement between calculation and statistical data in Figure 33 is only fortuitous, on the assumption that the field data must contain a number of records to ground wires which would vitiate the results. Actually, the data in Figure 33 were obtained on transmission lines which were not equipped with overhead ground wires. They were produced by positive polarity surges which did not cause flashover. The authors recognize that in the case of a stroke to a ground wire the initial portion of the induced stroke on a line conductor will, for a negative stroke, first have a predominantly high positive value, followed rapidly by a surge of negative polarity caused by coupling action.

The authors would like to discuss Mr. Golde's statement that "The tip of the return stroke is still in electric contact with the cloud and therefore, omitting the voltage drop along the leader channel, is practically at the cloud potential of several million volts." The authors picture the phenomenon as one in which the initial leader is a conductor of relatively high impedance, and that the return stroke is a process in which this poorly conducting path is converted into one of higher conductivity. Therefore, it would appear that the potential of the tip of the return streamer would lie somewhere between the actual potential of the cloud and ground, but, because of the higher conductivity of the return stroke, it would be much closer to ground potential than cloud potential.

The basis for the authors' statement that the return stroke velocities increase with current magnitudes is given in the paper in the section under "Velocity of Return Streamer." From a knowledge of the lower and upper range of velocities and similar data for stroke currents and the indications that return-streamer velocities increase with current, the simple assumption was made that these velocities could be segregated in the manner shown according to currents. Mean values were then assigned to each range. This segregation should be sufficiently accurate for the purpose.

In conclusion, the authors believe that, for the subject with which the paper is concerned, namely, the induced voltages produced by strokes to ground in the vicinity of a transmission line, the assumptions and analysis have been sufficiently justified in the paper. The paper does not consider the effect of strokes to the transmission line proper, whether the contact is made to either a conductor or a ground wire. In America, this type of stroke is usually regarded as a direct stroke. Most of the transmission-line design is based upon so locating the ground wires that they adequately shield the current-carrying conductors. Thus it is generally assumed that the stroke strikes a ground wire, and the resulting difference in potential between conductors and tower or ground wire is calculated, taking into consideration such factors as tower footing resistance, coupling factors, and so forth. It was not intended to discuss this type of stroke in the paper.

Practical Design of Counterpoise for Transmission-Line Lightning Protection

Discussion and authors' closure of paper 42-91 by E. Hansson and S. K. Waldorf, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, August section, pages 599–603.

Herbert B. Dwight (Massachusetts Institute of Technology, Cambridge, Mass.): The interesting measurements presented in the paper by E. Hansson and S. K. Waldorf, besides leading to the six general engineering conclusions listed in their summary, provide an unusually good check on ground-resistance formulas, and they show the practicality and desirability of making ground-resistance calculations.

Practical questions described in their paper, such as "What percentage reduction in tower-footing resistance can be expected by putting in one or more additional buried wires?" can be answered easily and reliably by the ground-resistance formulas.

In checking the measurements given in Table II and Figure 1 of the paper, the average resistivity of the ground was first computed from the average of the resistances of the seven individual buried wires. The result is 178,000 ohms per cubic centimeter. Then the various group resistances were calculated, the largest discrepancy between measurement and calculation being 11 per cent.

Table I

Distance Between Two Outer Wires (Feet)	Measured Reduction (Per Cent) Table II of Paper	Calculated Reduction (Per Cent)
10	7.1	7.3
20	8.5	9.8
40	9.6	12.7

Such computations should be made by any engineer installing buried counterpoise wires or groups of ground rods. In order to show how short and easy such calculations are, the first will be given in detail. Only two formulas are needed in this case, namely equations 17 and 18 of reference 9 of the paper.[1]

Average potential of one wire caused by its own charge (by equation 18)

$$=2q\left(\operatorname{logn}\frac{4L}{a}-1\right)=2q\left(\operatorname{logn}\frac{4\times 2{,}400}{0.125}-1\right)$$

$$=2q(\operatorname{logn}76{,}800-1)=2q\times 10.25$$

where q = charge per centimeter of wire. (For the ratio L/a use inch units, $L=200$ feet $=2{,}400$ inches and $a=0.125$-inch radius.)

Average potential caused by the image wire at $s=36$ inches (by equation 17)

$$=2q\left(\operatorname{logn}\frac{4L}{s}-1+\frac{s}{2L}-\frac{s^2}{16L^2}+\ldots\right)$$

$$=2q\left(\operatorname{logn}\frac{4\times 2{,}400}{36}-1+\frac{36}{2\times 2{,}400}-\ldots\right)$$

$$=2q(5.59-1+0.01-\ldots)=2q\times 4.60$$

Average potential of wire caused by itself and image $=2q\times 14.85$.

Total charge of wire and image, using the length in centimeter $=2\times 4{,}800\times 2.54q$.

$$\frac{1}{C}=\frac{\text{potential}}{\text{charge}}=\frac{2q\times 14.85}{2q\times 4{,}800\times 2.54}$$

By equation 14, reference 9

$$R=\frac{\rho}{2\pi C}$$

$$=\frac{\rho}{2\pi}\times\frac{14.85}{4{,}800\times 2.54}=34.5\text{ ohms}$$

$\rho=178{,}000$ ohms per cubic centimeter

By equation 17, potential caused by a wire at $s=40$ feet $=480$ inches is

$$2q\left(\operatorname{logn}\frac{4\times 2{,}400}{480}-1+\frac{480}{4{,}800}-\ldots\right)$$

$$=2q\times 2.09$$

Table II

Conductors	Ohms to Ground Measured	Calculated
1 and 2	18.5	17.4
3 and 4	18.8	17.4
1 and 4	14.1	13.9
2 and 3	13.2	13.9
All	10.1	9.8

The potential caused by the image of that wire $=2q\times 2.09$.

Potential when there are two wires 40 feet apart $=2q(14.85+2.09+2.09)=2q\times 19.03$.

$$\frac{1}{C}=\frac{2q\times 19.03}{4q\times 4{,}800\times 2.54}$$

Resistance to ground of two wires 40 feet apart

$$=\frac{178{,}000}{2\pi}\times\frac{19.03}{2\times 4{,}800\times 2.54}=22.1\text{ ohms}$$

The measured value, from Figure 1 of the paper, is 22 ohms.

Other cases calculated give curves of the same shape as those in Figure 1, and agreeing with them within 11 per cent or less.

The reduction in ground resistance by putting in a middle wire is shown in Table I of this discussion.

In a similar manner, the data in the last half of Table III of the paper can be computed from the average of the first four items, as shown in Table II of this discussion.

The practical check on the accuracy of the formulas is welcome. But, in future work, when an engineer wishes to know what percentage of change in ground resistance should be caused by a given change in counterpoise design, the information can be obtained more reliably by calculations of the type described than by comparing with previous tests. Such calculations are so short that they could be made in the field as soon as a measurement has shown that a tower-footing resistance is too high.

REFERENCE

1. CALCULATION OF RESISTANCES TO GROUND, H. B. Dwight. AIEE TRANSACTIONS, volume 55, 1936, December section, pages 1319–28.

L. A. Terven (West Penn Power Company, Pittsburgh, Pa.): The West Penn Power Company has in operation 3–132 kv single-circuit steel-tower lines which are provided with counterpoise, and one of them has been in service long enough to accumulate data to afford a comparison of lightning-protection characteristics with and without counterpoise. The line extends 58.8 miles from Lake Lynn on the border of West Virginia and Pennsylvania to Cumberland, Md., over a mountainous terrain with high-resistance substrata. The construction is horizontal with 20-foot separation between conductors and two ground wires are provided approximately ten feet above the conductor and separated 20 feet from each other. Because of the poor lightning performance of the line it was decided in the fall of 1936 to install continuous counterpoise between towers with unusually high footing resistance (the 17 towers affected averaged 171 ohms footing resistance). At the time six years of lightning season records had shown 15 interruptions. Some 9,000 feet of number 2 copper weld counterpoise was installed about 18 inches under the surface and was connected from tower to tower.

Table III of the discussion shows the lightning performance of the line before and after the counterpoise had been installed. The year 1937 is not considered, because the counterpoise was installed during the lightning season of the year.

Table III. Lightning Performance Before and After Installation of Counterpoise

Year	Interruptions Due to Lightning	Per 100 Miles Per Year	Remarks
1934	2	3.4	
1935	5	8.5	
1936	1	1.7	
1934–36 Average	2.67	4.5	
1937	1	1.7	Counterpoise installed during the year
1938	2	3.4	Quick reclosing in service from here on
1939	1	1.7	
1940	2	3.4	
1941	1	1.7	
1939 1940	*5	8.5	
1938–41 Average	2.75	4.7	

* Instantaneous tripouts which did not result in interruptions due to quick reclosing.

In the spring of 1939 quick reclosing breakers were installed at the line terminals which served to mask the performance of the line. Five instantaneous reclosures were registered in 1939–40 which were attributed to lightning, but which did not result in interruptions to service.

The table does not show any improvement in lightning protective characteristics caused by the counterpoise, the average interruptions per year per 100 miles being 4.5 before the counterpoise was installed and 4.7 after installation. Possibly the amount of counterpoise was not adequate, as a number of towers tested to 100-ohms footing resistance, which were not included in the counterpoise program, or perhaps the incomplete shielding of the conductors by the ground wires prevented the effect of the counterpoise being noticeable.

J. H. Hagenguth (General Electric Company, Pittsfield, Mass.): The authors give a very comprehensive study of the effectiveness of low ground resistance on the prevention of transmission-line flashovers. They have found by practical experience that the counterpoise is an excellent means of reducing tower-footing resistances to sufficiently low values to prevent flashover of line insulation where ground wires are used for all of the strokes contacting the towers or ground wires of 220-kv lines and in practically all cases of 132-kv and 66-kv lines. In the latter case, however, the tower current limit appears to be of the order of 60,000 amperes even for footing resistances of the order of six ohms. As stated by the authors, there appears some doubt with regard to actual tower currents, inasmuch as it has been found that currents in single legs are not necessarily equal, and further, that considerable percentages of the total tower currents may flow in the tower braces and therefore may not be measured with links installed at the tower legs only. Multiplication factors up to two have been found applicable, depending on the tower structure, to arrive at total tower currents from leg measurements only. In this way the authors calculate a maximum safe current of 120,000 amperes.

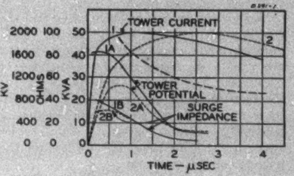


Figure 1. Counterpoise impedance (1B, 2B) and tower potential (1A, 2A) caused by tower currents (1, 2)

D-c resistance of counterpoise—six ohms. Plotted for two 200-foot counterpoises

I believe that our knowledge of the behavior of counterpoises, from theory and tests, is sufficiently accurate to permit calculations of actual voltages at a stricken tower for given current fronts and counterpoises installed. While it is not claimed that such calculations are very accurate, I believe that only by means of such calculations can we finally arrive at the true answer to some of the still unexplained phenomena. More accurate calculations can be made if any particular installation is considered where detailed data of counterpoise resistances are available.

The basis of the calculations is that the counterpoise is a variable impedance as shown by tests.[1] The time variation of this impedance depends principally on the length of the counterpoise and its d-c resistance. The counterpoise impedance varies with applied current wave front[2] as indicated by Figure 1 of this discussion. Multiplication of instantaneous values of current and counterpoise impedance results in the instantaneous tower voltage. The tower current is assumed to have a current peak of varying fronts and a tail of ten microseconds to half value on lines equipped with ground wires. The length of the tail of the tower-current wave depends essentially on the length of the span and the value of ground resistances of the towers. There is some evidence that the current peaks in stricken transmission-line towers are substantially of this order.

Figure 2 shows the crest values of tower voltages thus calculated as functions of tower-current wave front (solid lines) and the expected arc-over voltage of the insulator strings in use on the 66-kv lines described in the paper (635-kv negative 1.5×40 wave). The arc-over voltage curve is cor-

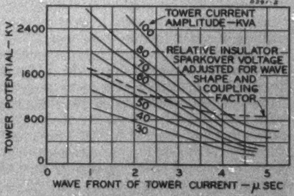


Figure 2. Maximum tower potential as function of wave front and amplitude of tower current for two 200-foot counterpoises connected to tower

D-c resistance of counterpoise—six ohms

rected for the type of voltage wave and the coupling factor given in the paper. Four parallel counterpoises of 200-foot length are used with combined d-c resistance of six ohms. The effective impedance used is the impedance of one counterpoise divided by four. This is not strictly true because of proximity effects between counterpoise wires in the same span, but suffices for the purpose of this example.

Figure 3 of this discussion shows a similar calculation for two 200-foot counterpoise wires with assumed d-c resistance of six ohms. Curves of this type show that the amplitude of the tower current, which will not cause flashover, greatly depends on the current front, as well as on the number of counterpoises installed. The tolerable current for a four-counterpoise ground is of the order of 1.6 times as great as for the two-counterpoise ground, even though the d-c resistance of both systems is equal. If, therefore, the tower current causing flashover on a two-counterpoise tower is 60,000 amperes, this would indicate that the current front of the tower current was of the order of not more than three microseconds. On the other hand, if the current was 120,000 amperes, the current front would have to be of the order of not more than 4.5 microseconds. Similarly, on a four-counterpoise tower, 80,000 amperes require a tower-current front of less than one microsecond and 120,000 amperes a front less than 3.5 microseconds. On this basis, a 60,000-ampere tower current should not cause sparkover on a tower grounded through four counterpoises.

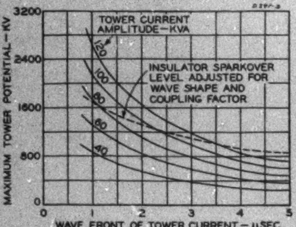


Figure 3. Maximum tower potential as function of wave front and amplitude of tower current for four 200-foot counterpoises connected to tower

D-c resistance of counterpoise—six ohms

It would be interesting if the authors could give in greater detail the conditions existing at towers of the 66-kv and 132-kv lines where sparkover occurred, the number of counterpoises installed and the probable amplitude of the tower current causing the flashover. A comparison of the data obtained in the field with curves of the type shown in Figures 2 and 3, would provide a valuable check between theory, laboratory tests, and actual lightning performance of counterpoise systems.

REFERENCES

1a. THEORY AND TESTS OF THE COUNTERPOISE. L. V. Bewley. AIEE TRANSACTIONS, volume 53, 1934, August section, pages 1163–72.

1b. COUNTERPOISE TESTS AT TRAFFORD, C. L. Fortescue, F. D. Fielder. AIEE TRANSACTIONS, volume 53, 1934, June section, pages 1116–23.

2. Discussion by J. H. Hagenguth of references 1a and 1b. AIEE TRANSACTIONS, volume 54, 1935, February section, pages 220–3.

F. O. Wollaston (Commonwealth Edison Company, Chicago, Ill.): Much of the information in this paper was made available to Commonwealth Edison Company by the authors and their associates for use in designing the counterpoise installation on the 220-kv Powerton–Crawford line. This information proved very helpful in the preliminary layout of our counterpoise system.

In the actual installation of counterpoises, which were of the discontinuous type, no practical means of predetermining counterpoise requirements was found. The procedure actually followed was to install successively wires up to 300 feet long, starting each wire at the tower. The combined resistance of tower footing and counterpoise wire was measured after each 100 feet of wire had been buried, and installation continued until the desired ohmic resistance was obtained.

Footing resistances were measured in all cases by the probe method, employing a Megger-type ground tester, and auxiliary current, and potential probes. While this method is satisfactory for determining the resistance of tower footings and driven grounds, it must be used with considerable care for counterpoise systems; otherwise some uncertainty will always exist as to the true ground resistance. The auxiliary grounds must be located far enough from the ground under test to eliminate errors caused by the large area covered by the grounding system. It will generally be found necessary to investigate the effect of varying the spacings of the auxiliary probes, and to determine the minimum distance at which accurate readings are obtained. For tower lines with extensive counterpoise system, the necessary distance to the current probe may be several hundred feet.

P. L. Bellaschi (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): An important characteristic of grounds is the lowering of the impulse resistance with current. This is particularly the case for grounds driven in high-resistance soil.[1]

Other important factors connected with the problem of grounds are the effect of the discharge-circuit or lead inductance, and the conditions in the earth. Conclusion 1 in the paper by Hansson and Waldorf and the field experience reported previously[2] focus attention on the inductance drop and its importance. The lead or tower inductance becomes particularly significant when steep-front currents of high magnitude are discharged to earth. For example: analysis shows that for a 2x40-microsecond, 40,000-ampere current the voltage developed in a 50-foot down lead or a 100-foot tower may well attain several times the ground-resistance drop (five-ohm resistance ground). This inductance drop is practically over in two microseconds.

Besides the lead or tower inductance, appreciable inductance may be present also in the ground proper, as is the case for grounds driven deep into the earth, or for a counterpoise spread over a wide area. Consider, for instance, a five-ohm 100-foot driven ground or counterpoise which taps its low resistance largely at the further end from the tower. The total inductance of the rod or conductor is in the order of 50 microhenrys, and, accordingly, this ground would have a time constant of about $L/R=50/5=10$ microseconds. A current rising to crest in a time shorter than this, naturally builds up a voltage across the ground which is the sum of the two drops, $L(di/dt)+Ri$. For currents rising rapidly to high crest values, the initial voltage developed across the ground is largely the inductance drop, and this can reach magnitudes considerably greater than the resistance drop corresponding to crest current. If the resistance is distributed along the length of the conductor or rod, the relationship of the two drops is more complex than previously stated—only a part of the total inductance of the conductor or rod is effective.

The foregoing are limiting factors. They do not, however, detract from the fact that deep grounds or counterpoise grounds, as the case and circumstances require, provide an easier path to the current discharge to earth.

REFERENCES

1. IMPULSE AND 60-CYCLE CHARACTERISTICS OF DRIVEN GROUNDS—II, P. L. Bellaschi, R. E. Armington, A. E. Snowden. AIEE TRANSACTIONS, volume 61, 1942, pages 349–63.

2. EXPERIENCE WITH PREVENTIVE LIGHTNING PROTECTION ON TRANSMISSION LINES, S. K. Waldorf. AIEE TRANSACTIONS, volume 60, 1941, June section, pages 249–54.

S. K. Waldorf and E. Hansson: It is not surprising that the performance of the transmission line described by Mr. Tervan was not improved by the addition of the counterpoise. The installation was made along only about three per cent of the line, and apparently the footing resistances of most of the towers in the line exceeded by a good deal the optimum footing resistances recommended in the summary of the paper under discussion. Undoubtedly Mr. Terven's analysis that the amount of counterpoise installed was not adequate is the fundamental reason for the lack of improvement in the line's performance.

It is interesting to note that Mr. Wollaston has also found field measurements as the only practical guide for counterpoise installation. Perhaps there are some localities where counterpoise characteristics can be satisfactorily predetermined, but Mr. Wollaston's experience around Chicago and that reported in the paper indicate that there are at least two widely separated territories where individual field testing of counterpoise is required during installation. Mr. Wollaston's comments on the spacing of auxiliary probes for ground-resistance measurements is welcome confirmation of the experience summarized in the first paragraph of section VI of the paper.

Professor Dwight suggests that the ground resistance of a counterpoise system can be calculated in the field and that the proper amount of conductor can be predetermined for a given location. This can occasionally be done, but field experience in the territory covered by the paper and that cited by Mr. Wollaston show that this is usually impossible. Changes in soil resistivity are frequently so large and abrupt that the resistance of 100 feet of wire is no sure criterion of the effect of the next 100 feet installed. Usually such changes occur most often when the counterpoise system passes from a wooded section into a cultivated field, or vice versa. A method of designing counterpoise systems was tried in which the required amount of conductor was calculated from the ground resistance of tower foundations, tower-foundation resistances themselves being a measure of soil resistivity. The field results differed so frequently and so widely from those predicted by calculation that the method was abandoned in favor of the cut-and-try or install-and-measure method.

The accompanying Table IV of the discussion complies with Mr. Hagenguth's request for more complete details about towers at which flashovers have occurred and at which counterpoise was installed.

Table IV. Flashovers on Transmission Towers Provided With Counterpoise

Line and Tower	Tower Current in Amperes	Ground Resistance of Counterpoise in Ohms	Tower Potential (I×R)	Type of Counterpoise Installed
Holtwood–York Tower 63 (66 kv)	58,400	13.8	805 kv	Two 1,075-ft conductors west to next tower, two 425-ft conductors east to next tower
Holtwood–York Tower 145 (66 kv)	60,000	19	1,140 kv	Two 500-ft conductors west to next tower, two 425-ft conductors east to next tower
Holtwood–Coatesville Tower 40 (66 kv)	64,400	17.2	1,110 kv	Three 400-ft conductors west, four 600-ft conductors east to next tower
Holtwood–Coatesville Tower 97 (66 kv)	60,200	5.4	325 kv	Two 750-ft conductors west to next tower, three 400-ft conductors east
Safe Harbor–Perryville Tower 15 (132 kv)	52,600	12.2	640 kv	Four 300-ft conductors north, four 825-ft conductors south to next tower

The Holtwood–York 66-kv line with a coupling factor of 0.35 gives an insulating level of about 980 kv. On the Holtwood–Coatesville 66-kv line the corresponding values are about 0.25 and 850 kv, and on the Safe Harbor–Perryville 132-kv line they are about 0.35 and 1,550 kv. The counterpoise systems at the towers of Table IV are all arranged in simple patterns such as shown in Figure 2 of the paper. The tower currents were in every case either the sum of currents measured in the four corner legs of the tower or the current measured in one corner leg multiplied by four.

Mr. Bellaschi's analysis of the behavior of counterpoise and long ground rods helps one to understand the phenomena involved when flashovers occur with low measured ground resistances.

Effect of Lightning on Thin Metal Surfaces

Discussion and authors' closure of paper 42-104 by K. B. McEachron and J. H. Hagenguth, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, August section, pages 559–64.

M. Newman (University of Minnesota, Minneapolis, Minn.): The paper by K. B. McEachron and J. H. Hagenguth presents several very interesting cases of lightning to spheres on top of radio towers, where many of the strokes went to the lower half of the sphere with the top of the sphere apparently shielded to some extent. It would seem that an explanation of this effect could be advanced on the basis of relative gradient distribution about the sphere portion and the supporting shank. Simple approximate gradient calculations indicate that the gradient about a small radius shank could easily exceed the gradient about the much larger radius sphere itself, especially when the opposite electrode, in this case the cloud, is far removed so as to contribute little relative increase of gradient at the top of the sphere. The writer recalls a photograph in a British publication which shows the case of a 60-cycle discharge which took place around the spheres when they were widely separated, and, while no explanation was given with the photograph, it would seem that the considerations submitted by the writer of this discussion of relative gradients about the sphere and support would fit both the 60-cycle case referred to and the case of the long-duration lightning discharges occurring to the lower portions of the sphere.

It would be of interest to have more complete information on the details of the support and whether any pits were visible on the support itself. It would seem probable that many of the streamers would extend from the supporting shank slightly below the shielding zone of the sphere with a possibility of the completed discharge path shifting to the shorter paths to the sphere surface.

McEachron and Hagenguth are to be greatly complimented on presenting in their paper a great deal of new interesting data which lead to new information. It is hoped the new data will be clarified further in this and other discussions. The points in the present discussion are submitted, because they have a bearing on questions of shielding theory and also on the question of whether the discharges necessarily originated from the sphere.

P. L. Bellaschi (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): I agree with the authors that the fusion found on metallic objects hit by lightning is a source of useful information. For instance, initial information of this character from electric conductors provided a valuable guide during the early laboratory research on long-duration low-current components of lightning strokes, so that important developments on protective devices and other electrical apparatus were well under way in 1938.

From the data discussed in a paper on this subject, which I presented to the Institute a year ago (reference 2 of this paper), the mean value of the charge in lightning strokes to electric-power lines, lightning rods, and to other objects near the surface of the earth was estimated to be between 15 and 30 coulombs. The maximum charge observed was 200 coulombs, but there is evidence from the high volume of fusion that this charge might reach 300 coulombs in rare cases. Although the data presented by the authors may differ somewhat from that obtained by the writer in regard to the methods of test and of analysis, it appears to me that the results seem to be in fairly close agreement.

In addition to their basic interest, these investigations are of direct practical value as pointed out by the authors in determining the minimum thickness of metal for containers of flammable or explosive materials. The question has been asked, "What minimum-size conductor can be used on overhead lines that will not be burned in two by lightning?" According to my data for copper, a number 4 conductor should be satisfactory in most cases. A few cases of larger conductors having been sundered by lightning have been reported, but the evidence is not entirely conclusive. In addition to the heating effect of the stroke, consideration must be given to the mechanical forces exerted on the conductor by the action of the high-current component.

The fusion on metals and the various effects associated with the high-current component provide about the only clues that can be found at the scene to assess the nature and severity of the lightning stroke and to reconstruct the probable or true circumstances in a particular case.

K. B. McEachron and J. H. Hagenguth: With regard to Mr. Newman's question concerning pits on the sphere supports, no information is available for the *WSM* sphere. We have recieved another similar sphere from *WLW*, which had been mounted 828 feet above ground for approximately seven years, with a five-inch-long portion of the shank. The sphere had a 14-inch outside diameter and 23-mil thickness of copper. The inside diameter of the shank was $1^9/_{16}$ inch and the outside $1^7/_8$ inch, with a wall thickness of the shank of $^5/_{32}$ inch. There are 122 pits on the shank. The distribution of these pits along the shank is of interest, being 28, 41, 36, 14, 3 pits per one-inch band, starting from the sphere. This would indicate that the number of pits at distances of six inches and greater from the sphere should be negligible. While the theory of gradient distribution is attractive, it is very difficult to state whether the shank is hit and the arc travels to the spheres or whether the sphere is hit and the arc moves down the shank. Because the pits on the shank are numerous only close to the sphere, it should be expected that the sphere would shield that portion of the shank and the stroke channel moves down toward the shank.

The distribution of the holes shows a similar concentration in the center belt as for the *WSM* sphere, about 52 per cent of the holes being found in 42 per cent of the six-inch-wide zone at the equator. Clerk Maxwell shows the lines of force (Figure III, article 120)[1] caused by a charged point in a uniform field. If we use one of the nearer equipotential surfaces as a metallic surface, we obtain practically a sphere. The gradients there would correspond to the location of the hole. The shank, of course, should upset this field. The rapid decrease in pits on the shank away from the sphere, as well as the distribution of pits and holes in the center belt of the sphere, would indicate that the gradients at the sphere are substantially the same with shank as shown by Maxwell without the shank.

There were a total of 695 pits on the sphere and the shank, 27 holes in the upper half, and 29 holes in the lower half. The largest hole indicated 200 coulombs and was located in the lower half. The expectancy curve checks very closely the curve obtained from the *WSM* sphere, 50 per cent of the holes indicating about 20 coulombs.

Information such as obtained by holes burned in metal and fusion of conductors is, of course, only a very rough indication of the total charge involved. The current values and duration cannot be obtained by such methods. Only the oscillograph or other devices which can measure both time and current amplitude can give accurate results. Oscillographic evidence of the long-duration discharges were first obtained in 1937 at the Empire State Building and reported in 1939 by McEachron.[2] Evidence of fusion in the field is valuable, however, because it shows that long-duration discharges exist not only on tall structures such as the Empire State Building but also on distribution circuits, dwellings, and other structures of relatively low elevation above the earth's surface. There is no reason why results of the tests presented in the paper should not check with Bellaschi's findings on fusion of lightning rods. Combination of such data will result in a better quantitative understanding of the long-duration discharges.

REFERENCES

1. ELECTRICITY AND MAGNETISM, Clerk Maxwell. Volume 1. Clarendon Press.

2. LIGHTNING TO THE EMPIRE STATE BUILDING, K. B. McEachron. *Journal* of the Franklin Institute, volume 227, February 1939.

Transient Recovery Voltages and Circuit-Breaker Performance

Discussion and author's closure of paper 42-120 by R. C. Van Sickle, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, November section, pages 804–13.

J. A. Elzi (The Commonwealth and Southern Corporation, Jackson, Mich.): Circuit breakers used as switching devices for equipment such as motors, capacitors, line-charging reactors, and synchronous-condenser starting present one of the most difficult application problems, and breaker performance for these applications has frequently not been satisfactory. This is evidenced by excessive contact burning and distress during operation, both of which are an indication of improper co-ordination between rate of transient recovery voltage and the rate of recovery of dielectric strength between the breaker contacts. It frequently happens that these breakers are nonautomatic in their operation or that the short-circuit duty is quite low, so that interrupting rating is not a suitable criterion for the selection of a breaker.

The results of the tests presented in this paper and also the data presented in the companion papers indicate that, even though the rate of transient recovery voltage may be relatively high, it is possible to select a type of circuit-interrupting device which will give trouble-free performance at the relatively low values of current which the breaker is required to interrupt in these applications.

In the past it has been the practice to apply a breaker having a higher interrupting capacity as a remedial measure where such troubles have been encountered. This method has met with some measure of success, but, judging from the results presented in this paper, it appears that a far better approach in making these applications will consist in the proper selection of the type of interrupting device used rather than in simply providing a larger breaker.

F. Von Voigtlander (The Commonwealth and Southern Corporation, Jackson, Mich.): The author shows that the extremes of transient recovery voltages are higher than had been anticipated, but that generally the values actually encountered in practice are much lower. Companion papers have shown that but few circuit breakers have failed in service caused by high transient recovery voltages; yet many circuit breakers now in service were designed before complete data were available on these phenomena.

The writer of this discussion would therefore like to ask whether the present switchgear may not be overdesigned in this respect, possibly at the expense of other desirable characteristics, and whether worthwhile economies or improvements of other features might not be possible by taking into account the lower recovery rates usually experienced, even though such a procedure might require measures to limit extremes in certain special applications.

Raymond C. R. Schulze (Public Service Electric and Gas Company, Newark, N. J.): Published data in the past have indicated that the recovery rate had a definite effect on the interrupting capacity, but there have been no co-ordinated data or series of tests on this problem. This paper by Mr. Van Sickle supplies some much needed information on the subject.

When I was employed by Public Service Electric and Gas Company my first job was to calculate short-circuit currents and to compare circuit-breaker duties with their ratings. I have had a hand in this job ever since. About the time that we appeared to be up-to-date, something was added to the system, or the manufacturer derated his breakers, and we had to start over again. Sometimes there are too many circuit breakers to be rebuilt or replaced for the money available in any one year, and so it becomes necessary to pick out the worst ones. This is done by comparing the short-circuit amperes, the probability of operation, and the experience on the various breakers. The experience consisted of the recovery-voltage rate and certain miscellaneous factors; before the appearance of Mr. Van Sickle's paper, it was felt that, if the time were available, the recovery voltage rates for the various breakers could be calculated, and then this factor could be separated out of the experience. However, this paper seems to indicate that for oil circuit breakers the arcing time is substantially independent of the recovery voltage rate. (This is above some critical value of recovery rate which is relatively too low to be troublesome.) This is indicated by various figures and statements in the paper. If true, this is fine, because if the arcing time is not appreciably affected by the recovery voltage rate, then there should be no real need for calculating this factor. This should save a lot of work.

On page eleven of the paper, the statement is made that "Published data has frequently shown that breakers with self-generated deionizing action have shorter arcing times at high currents than at low currents." We have had some experience to substantiate this statement. One manufacturer tested one breaker, of which we have a number, and which we felt was rated too conservatively. The rating we finally adopted for this one breaker is 140 per cent of its name plate value, although this particular breaker actually withstood up to 280 per cent of its name plate rating. It has been our practice to replace these breakers when the duties exceeded 140 per cent (when some other work was being done on equipment associated with it). In view of the statement in the paper, and the fact that the breaker actually took more than our rating, perhaps we have spent a lot of money unnecessarily.

Another point in Mr. Van Sickle's paper interests me. Various figures concerning oil breakers show that the arcing time is relatively constant with an increase in either short-circuit current or recovery voltage. However, the arcing time in a compressed-air breaker does increase as the recovery voltage rate increases. I am wondering if this might be an argument for continuing to use oil breakers rather than the compressed-air type.

Considering the data which Mr. Van Sickle must have on the varying rates of decay of current at various natural frequencies, and the value of a curve of decrement factor versus natural frequency for use in future recovery voltage calculations, I am wondering if Mr. Van Sickle could develop such a curve for the use of the industry.

David C. Prince: For discussion, see page 1016.

R. L. Webb (Consolidated Edison Company of New York, Inc., New York, N. Y.): All of the papers just presented on the subject of transient recovery voltages as applied to power switchgear should prove informative and useful to both users and manufacturers of circuit breakers.

The paper giving a "Practical Calculation of Circuit Transient Recovery Voltages" provides a simplified method of calculation that can be used with reasonable accuracy by engineers not expert in this particular field. The method was used by us in assisting with the Association of Edison Illuminating Companies survey, and it was found relatively easy to follow. The data furnished on circuit constants are essential.

The paper on "Transient Recovery-Voltage Characteristics of Electric-Power Systems" shows that recovery rates above 5,000 volts per microseconds may be expected on only a small percentage of all breakers. We do not know yet whether further developments in some breakers may be proved essential for satisfactory performance at recovery rates approximating 10,000 volts per microseconds, but utility engineers can give some thought to means for keeping the recovery rates low.

High recovery rates are usually associated with a circuit of very low capacitance connected between a breaker terminal and a lumped reactance, such as a reactor or a transformer. On one such circuit of our 27-kv system the recovery rate was calculated to be 8,600 volts per microseconds. In experimenting with the calculations we found that this rate could be reduced to less than 5,000 volts per microseconds by connecting a five-foot length of lead-covered cable in series with, or as a stub to, the low capacitance part of the circuit. In this case the cable added about 750 micromicrofarads to the capacitance of the connection. Some such correction might be found for any circuit.

It appears, therefore, that the problem of relieving stresses on switchgear resulting from high recovery voltage rates rests with the user as well as with the manufacturer. The user can lay out his connections initially, in many cases, so as to avoid the type of circuit which inherently gives high recovery rates.

It is encouraging to see the extent to which new testing facilities for high voltage-recovery rates have already been used to check the performance of both oil and air circuit breakers. The paper "Transient Recovery Voltages and Circuit-Breaker Performance" describes laboratory tests at the highest recovery rates used so far in test circuits. The limits reached in voltage-recovery rate seem satisfactorily high for proving breaker performance, in view of the upper values found in the AEIC survey. The tests would be more conclusive, however, if high current at rated voltage could be interrupted with associated high recovery voltages applied to the breaker terminals on the same test.

C. Concordia and **H. A. Peterson** (General Electric Company, Schenectady, N. Y.): These papers on transient recovery voltages present a clear picture of the recovery rates which may be encountered in actual circuits, of methods of calculating them, and of the effect of these recovery voltages on circuit-breaker performance for the simplest form of test circuit. However, a review of actual circuit-breaker performance indicates that factors other than the rate of rise of recovery voltage may, in particular instances, have a greater effect on breaker performance; that is, the rate may be low and yet the interruption difficult.

The conclusion brought to light by several recent analytical studies is that the configuration of circuits containing appreciable amounts of capacitance may make them much more severe than the simple circuit considered as the laboratory test. For example, high reactance grounding[1–3] may reduce the recovery rate and at the same time increase the severity. Again, interruption of line charging current has a very low associated recovery voltage but is known to be a severe duty.[4]

The points above are worthy of emphasis, because they are not merely special cases. Rather, one or more phases of a breaker may be required to interrupt a small current with a slow recovery voltage rate since unbalanced faults are more prevalent than three-phase faults. This small current may be either capacitive in nature (similar to line charging current) or reactive, depending upon the assumed condition, phase-clearing sequence, and system grounding impedance. Consequently the foregoing points are essential factors in the development and in evaluating the performance of any breaker.

References

1. Effect of Restriking on Recovery Voltage, **C. Concordia, W. F. Skeats.** AIEE Transactions, volume 58, 1939, August section, pages 371–5.

2. Arcing Faults in Power Systems, **C. Concordia, H. A. Peterson.** AIEE Transactions, volume 60, 1941, pages 340–6.

3. Critical Analysis of Rotating-Machine Grounding Practice, **H. A. Peterson.** *General Electric Review*, April 1942, pages 215–20.

4. Circuit-Breaker Recovery Voltages, **R. H. Park, W. F. Skeats.** AIEE Transactions, volume 50, 1931, March section, page 212.

A. J. Krupy (Commonwealth Edison Company, Chicago, Ill.): As a general comment upon the three papers relating to the system recovery voltages and their effect on the performance of the circuit breakers, I would like to stress a point which was briefly mentioned in the Adams–St. Clair paper and also give some data on the service experience with the breakers which may be of interest to MacNeill and Van Sickle and to others:

When the voltage-recovery calculations were completed and the results analyzed, it was found that a fairly large percentage of the system breakers were subjected to a high recovery voltage. From this, it would appear that considerable trouble should be experienced with the circuit breakers operating under such voltage-recovery conditions. However, in case of our system, a careful check of the service performance of breakers during the period of some 20 years showed that there was not a single case of breaker failure or even any serious distress which could be definitely traced to a high rate of voltage recovery. Such breaker failures as did occur were invariably confined to the breakers with the previously known inadequate interrupting capacity, or to those which failed because of some other clearly established reasons.

This leads us to the conclusion that a substantial well-designed breaker, which has an ample thermal capacity and a sufficient interrupting duty for a given location, has given us a remarkably satisfactory service even at the locations which in calculations gave rather high recovery voltages.

The above service record may be explained in several ways:

1. The calculations were made for the most pessimistic system conditions which exist only a small fraction of the total service time, and, therefore, the calculated results are unduly pessimistic.

2. A three-phase ungrounded fault, which was assumed in calculations, is a very rare occurrence as compared with the variety of other faults experienced on the system.

3. The breaker itself reduces the voltage-recovery rate to an appreciable extent especially for the high values of current.

4. A breaker which was designed to obtain high interrupting capacity inherently possesses a sufficiently good voltage-recovery characteristic.

5. During the past 20 years, our system has been persistently lucky.

It is most likely, of course, that not only one but several of the above factors were responsible for the observed service record. This does not mean, however, that the work and the tedious calculations which have been made in connection with the survey are of no practical value. On the contrary, in case of our system, for example, it was plainly demonstrated that only a very minor change in the design of a switch house, or a minor change in the connection between the reactor and the breaker, can reduce the initial rate of voltage recovery to a fraction of its original value. Thus, we have found an easy and simple way to control to some extent the voltage-recovery rate at a given location, should conditions arise which would warrant it.

J. B. MacNeill (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The group of three papers on transient recovery voltages form a symposium covering system conditions and circuit-breaker design aspects. This is a valuable contribution and should correct certain wrong impressions which have grown up over a period of years, and which have been associated with the inadequate performance of early switchgear for difficult restored voltage conditions.

The results of the Association of Edison Illuminating Companies study and the work done by manufacturers in their laboratories, as shown by the Van Sickle paper, may be summarized as follows:

1. Extremely high rates of recovery voltage are rather unusual under normal operating conditions.

2. Modern circuit interrupters are not sensitive to recovery voltage rate as were earlier devices. In fact the average modern interrupter will handle without distress, a broad band of recovery rates.

3. The occasional extremely high-speed rate of recovery does not affect a large system area, and, if necessary, means of slowing down the recovery rate are generally available.

The modern oil circuit breaker fortunately handles widely differing rates of recovery without its performance being affected greatly. This, I believe, is due to the nature of the dielectric in the arc stream during interruption. It cannot be assumed, however, that other forms of interrupter will necessarily have the same latitude in handling recovery voltages. Radically different forms of interrupter will need to be verified in this important respect. In general, however, laboratory testing equipments present restoration rates higher than average operating conditions. With the knowledge now available on system conditions, design engineers should be able to provide adequately against extended arcing times caused by any reasonable rate of voltage recovery.

R. C. Van Sickle: The discussions of these papers have brought out a number of important points which arise in interpreting the data for circuit-breaker application.

The scope of the data needs emphasis. The tests were made to determine the effect of varying the transient recovery voltage while the current interrupted and the normal frequency recovery voltage were held constant. To obtain the high natural frequencies associated with the most severe transient recovery voltages reactors were placed in the test cells adjacent to the breaker. They limited the current which could be used in this type of testing. These tests fulfilled their purpose by demonstrating the characteristics of the transient recovery voltages which are found with the maximum arcing times. These characteristics were found on the normal testing circuits at the highest currents.

The data demonstrated that at a given voltage and current, increasing the severity of the transient recovery voltage increased the arcing time up to a limit and that further increase in the severity did not further increase the arcing time. This limit was reached well below the values obtained on normal testing circuits.

To fully demonstrate a breaker for a given voltage and current it is necessary to test it with a transient recovery voltage which produces the maximum arcing. A test or service on a circuit which produces less than the maximum arcing can demonstrate the breaker for that service, but the breaker might fail completely if applied on a more severe circuit.

A circuit breaker passing the high-power laboratory test should perform as well or better in service. It does not follow that the breakers are designed with too high a factor of safety for the majority of applications. As shown by the data submitted in this paper, the maximum arcing time of the breakers tested was reached at relatively low natural frequencies of the transient recovery voltage. The voltage-recovery rates corresponding to them can be obtained on over 70 per cent of the breaker locations of the 11,000 to 13,800 volts range as shown by Figure 10 of the paper by St. Clair and Adams. Of the remaining 30 per cent, part will be on circuits which can cause almost the maximum arcing time. A few will be on circuits where they can never be severely stressed. These few might be built less ruggedly but would be hazardous if moved to other circuits requiring the same interrupting capacity but having more severe transient recovery voltages. The few which could safely be applied for light duty only would hardly justify the development and maintenance of a second line of breakers. If other types of breakers are found to have their performance more dependent on the transient recovery voltage, this conclusion might not hold for them.

The data of the survey and the breakers' performances indicate that the majority of the circuit breakers can be subjected in service to conditions which produce their maximum arcing times. The normal testing circuits of the high-power laboratory have fortunately been sufficiently severe to require breaker designs adequate for any service condition.

The arcing time of the compressed-air breakers tested varied less with transient recovery voltage than did the arcing times of the oil breaker. Figure 4 shows that the arcing time of the plain-break oil circuit breaker varied from 0.5 to 3.8 cycles. Figure 9 shows that the arcing time of the de-ion grid breaker varied from 0.3 to 2.1 cycles. Figure 14 for the 1,500,000-kva compressed-air circuit showed no significant difference in the arcing time at 8,000 amperes and an average increase about three-fourths cycle at 36,000 amperes. The cathode-ray oscillograms of the tests at higher current indicated that the maximum arcing time had been reached. As stated in the text, varying the transient recovery voltage rate on the 2,500,000-kva breaker from 300 to 13,600 volts per microsecond at 8,000 amperes 13,200 volts varied the minimum and maximum arcing times by only one or two tenths of a cycle.

The rate of decay of the current in the transients during these tests appears to be approximately proportional to frequency and corresponds closely with the factor used in the survey. Using capacitors for loading the circuit decreased the rate and using transformers for loading the circuit increased the rate, but the variations were relatively unimportant and demonstrated that the factor was satisfactory.

Transient Recovery-Voltage Characteristics of Electric-Power Systems

Discussion and author's closure of paper 42-130 by H. P. St. Clair and J. A. Adams, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, September section, pages 666–9.

W. F. Sims (Commonwealth Edison Company, Chicago, Ill.): As pointed out by Mr. St. Clair in the presentation of his paper, the papers on this subject are based on a survey of voltage-recovery rates on six large utility systems conducted by the committee on electric switching and switchgear, Association of Edison Illuminating Companies. Conditions leading up to this survey should be of interest in consideration of the information contained in these papers.

In 1935 a report on circuit-breaker testing was prepared by the oil circuit-breaker testing subcommittee of the committee on electric switching and switchgear, AEIC, in which it was pointed out that there was insufficient information available in reference to circuit-breaker transient recovery voltage, and it was recommended that a survey be made to obtain this information.

This subject was under consideration by the committee for a considerable time, and in 1939 steps were taken to make the survey, which was assigned to the oil circuit-breaker testing subcommittee under the chairmanship of Philip Sporn. During this period there was considerable discussion with the manufacturers' engineers, and it was brought out that information of this nature was quite important and would be of considerable value in working out of circuit-breaker designs.

As a result of these discussions six large member companies of the association, whose properties were representative of both the metropolitan type and long transmission type of systems, agreed to take part in the survey and to make the necessary calculations on their respective systems. The actual work was carried out by a working group of engineers, one from each of these companies, under the direction of H. P. St. Clair. As J. A. Adams had already done considerable work of this kind on the system of the Philadelphia Electric Company, arrangements were made to secure his services in standardizing the methods and co-ordinating the calculations made on the various systems in order that all work would be done on the same basis and that the data obtained would be comparable. Much valuable assistance was given to the working group by the engineers of the switchgear departments of the large manufacturers.

The results of this survey are described in detail in a report to the committee on electric switching and switchgear presented in the spring of this year. As the information developed appeared to be of considerable importance permission was given to the protective devices committee of the AIEE to arrange for the preparation and presentation of these papers at this session.

David C. Prince (General Electric Company, Schenectady, N. Y.): For many years it has been recognized that the rating of circuit breakers in volts and amperes is not complete. To a considerable extent, the severity of duty is a function of the recovery voltage rate at the point of application. Manufacturers have been handicapped by having to work very largely in the dark, from a recovery-rate point of view. That is, circuit breakers had to be designed and built capable of handling the maximum recovery rate which might be encountered, although the designers had no information whatsoever on what that recovery rate might be.

This uncertainty has now been dispelled once and for all by the studies on which St. Clair and Adams are reporting.

Prior to receiving this information, we have speculated with the possibility that low-recovery-rate circuit breakers might be generally applicable with high-duty circuit breakers for only a few isolated cases. The evidence in this paper is that a fairly large fraction of all circuit breakers may be exposed to high recovery rates. In a way, this is disappointing, since it closes the door on the low-duty breaker. On the other hand, from this time on, circuit-breaker designers will know the duty which their products must meet, and it is only reasonable to expect that with definite knowledge available, ways will be found to tailor the product exactly to the need and in that way reduce the cost to the consumer for a given service rendered.

The data presented by R. C. Van Sickle do not really negate this conclusion. The factors in circuit-breaker design which cause the circuit breaker in operating to alter the rate at which recovery voltage appears involve the dissipation of energy in the circuit breaker itself. In the cases mentioned by Mr. Van Sickle, the kilovolt-amperes interrupted have been small, and the presence or absence of auxiliary losses which altered the recovery rate consequently unimportant. Where higher interrupted capacities are involved, the conclusion might well be different, and it is to be hoped that with the splendid new high-capacity testing station available to him, Mr. Van Sickle will extend his work in that direction.

R. L. Webb: For discussion, see page 1014.

C. Concordia and **H. A. Peterson:** For discussion, see page 1015.

A. J. Krupy: For discussion, see page 1015.

J. B. MacNeill: For discussion, see page 1015.

J. A. Adams: Mr. Webb and Mr. Krupy mention one of the benefits of the work which has been done on recovery voltages: namely, to suggest to users of circuit breakers that the severity of the recovery voltage characteristics of circuits can be reduced by changes in station design. However, as a word of caution, it should be pointed out that the introduction of lead-covered cable, as mentioned by Mr. Webb, or of capacitors may introduce an additional hazard in some cases. This may be especially true in the case of a switch house where the breakers are located next to the bus with reactors on the line sides of the breakers. If the breakers have been applied on the basis of breaker duties determined for faults beyond the reactors, it is essential that the circuits between the breakers and reactors be made as reliable as possible. Additional capacitance inserted in this portion of the circuit should be obtained in as reliable a manner as possible.

In connection with the Association of Edison Illuminating Companies survey an attempt was made to co-ordinate recovery rates with breaker failures such as was done by Mr. Krupy. This would have given an indication of the effect of recovery rates on breaker operation. However, records in some cases were incomplete, and in others time did not permit the analysis to be made. It is reasonable to expect that in the future this factor will be more carefully studied for its effect on breaker operation.

Mr. Krupy and also Mr. MacNeill point out that actual fault conditions may not result in as severe recovery voltage characteristics as the calculations indicate. This is true, in some cases, because the system connections may not be the same as those for which the calculation was made, and in others, because the fault may not be a three-phase fault or may not be located at the station line terminal. However, the most severe recovery voltage conditions that can be obtained with operating connections should be recognized when applying a breaker. In the survey the calculations were based on connection which gave the most severe characteristics under normal or emergency conditions. In a great many cases these were the connections used for normal operation. Also the calculations were for the highest three-phase fault current that could be obtained for the connections used. In actual practice, the fault might only be phase-to-ground or phase-to-phase and might not be located at the line terminals, both of which factors would tend to reduce the severity of the recovery voltage characteristic.

Mr. Prince refers to the possibility of tailoring breakers to the need and thus reducing costs. It is sincerely hoped that the results of this survey will be of assistance in this respect.

Practical Calculation of Circuit Transient Recovery Voltages

Discussion and authors' closure of paper 42-131 by J. A. Adams, W. F. Skeats, R. C. Van Sickle, and T. G. A. Sillers, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, November section, pages 771–9.

G. W. Dunlap (General Electric Company, Schenectady, N. Y.): In the consideration of transient recovery voltages there are four general methods by which the form of such voltages may be obtained for a given circuit or system. These may be listed as:

1. Direct oscillographic measurement during actual fault interruption.
2. Mathematical analysis.
3. Electrical analysis of miniature circuit representing actual circuit.
4. Electrical analysis by measurements on actual circuit.

All of these methods have their limitations and advantages, and all have been used to obtain valuable information and must be considered as supplemental to each other. The authors have presented in their paper a new and simplified technique for the second method which should greatly increase its usefulness, and it is the purpose of this discussion to point out how the advantages of methods 3 and 4 may be readily made available to supplement calculations by the use of the simple device called the recovery voltage analyzer, which is described in the author's reference 10.

This recovery voltage analyzer may be used on either actual circuits or miniature representations thereof. It performs electrically the same trick of injecting a current into the circuit under study that is used mathematically as the basis for calculation of transient recovery voltages, and, in case of the analyzer, the answer appears on the screen of a cathode-ray tube as a stationary trace of the transient recovery voltage desired. In other words, with the analyzer connected to the circuit for which the curve of Figure 11 was calculated, the Figure 11 curve would appear on the cathode-ray tube to be observed, measured, or recorded photographically as desired.

As pointed out in the paper, it is extremely important that the capacitances associated with higher-frequency circuits be estimated accurately, and in such cases the analyzer may be used to advantage, not only to check calculations based on estimated capacitances with actual measurements of recovery transients, but to measure these capacitances as well. In addition, since the analyzer does not care how complicated a circuit may be, such measurements may be used to check the validity of the simplifying assumptions made for purposes of calculation. The analyzer has been used to make such measurements on a number of power systems. Wherever it is possible to de-energize a circuit for analyzer application the actual measurement is accomplished with such speed and facility that the time of calculation may be saved, and this has also been done in a number of cases.

This feature of speed brings up the possibility of using the analyzer to eliminate the routine work of calculating points on the recovery transient curve. The simplified lumped constant circuit of Figure 10, for example, could be connected to the analyzer, and by the throwing of a switch the recovery transient could be made to appear in its complete form on the cathode-ray tube, and the envelope or any portion of the wave desired could be measured. If there were any doubt as to the validity of estimated capacitances, the values could be varied over any desired range, and the effect on the recovery transient would be immediately apparent.

Still another use of the analyzer is in determining the characteristics of individual circuit elements such as generators, transformers, reactors, and so forth. In this way values of natural frequency, equivalent inductance, and capacitance may readily be obtained for subsequent use in calculation of a system as a whole.

In conclusion, it would appear that the usefulness of the method described by the authors may be further enhanced by including the feature of electrical analysis as embodied in the recovery voltage analyzer.

W. F. Sims: For discussion, see page 1016.

R. D. Evans (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors deserve commendation for the methods of recovery voltage calculation and the data on capacitance that they have presented.

For network problems of the complexity treated in the paper the solutions are not rigorous, and only reasonable engineering accuracy is intended. Bold approximations have been used by the authors to reduce the networks to a more manageable form, for which rigorous and practical solutions are available. These approximations will give good accuracy when skillfully used. Nevertheless, a word of caution is desirable for the benefit of those who are unfamiliar with such calculations but have occasion to use the methods presented in the paper; unless

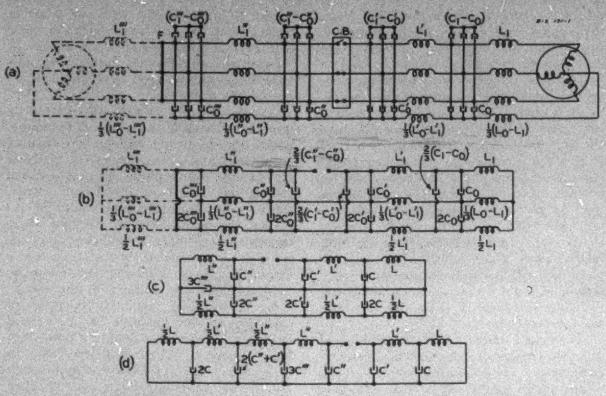


Figure 1. Equivalent circuits for recovery voltage calculations

Symmetrical-component presentation

Network simplifications

the approximations are permissible for the particular case, the accuracy will be considerably reduced.

For setting up the network for recovery voltage calculations, there are several possible methods, including the single-phase method used by the authors. We, however, prefer to start with a netowrk based on symmetrical-component methods, such as described by Evans and Monteith.[1] This is a general method for setting up networks and permits representation of circuit elements which may be unequal for the positive-, negative-, and zero-sequence circuits. This method is convenient because of the practice nowadays of calculating the constants of circuit elements by symmetrical components. The labor of the symmetrical component method is about the same as that of the single-phase method for the cases where the latter is applicable.

One method of using symmetrical components to set up the equivalent circuit for recovery voltage calculation is shown in Figure 1a of this discussion with the inductances and capacitances indicated by the usual notation. This network corresponds to the system of Figures 4 and 5 of the paper but is presented in a more general form which permits differences in the positive- and zero-sequence inductances and capacitances of the various circuit elements. In addition, there is provided a path for zero-sequence current through the feeder beyond the fault, as shown by the dotted line section of the figure. For the opening of the first pole of the circuit breaker for the case of a three-phase ungrounded fault, it is permissible to parallel the two remaining phases which reduces the network of Figure 1a to that of Figure 1b. If it is assumed that the positive- and zero-sequence inductances, and the positive- and zero-sequence capacitances of the various circuit elements are equal, and if the zero-sequence path through the feeder beyond the fault is neglected, the network of Figure 1b reduces to that of Figure 1c, in which the subscripts have been dropped for convenience. By simple rearrangement, the network of Figure 1c reduces to that of Figure 1d.

The network of Figure 1d may be simplified in various ways to reduce it to a more manageable form for calculation. The authors do this by:

1. Reducing the left-hand part to three meshes by combining capacitances $2C$ and $2(C''+C')$ (ignoring the latter) and combining the inductances $^1/_2L$ and $^1/_2L'$.

2. Reducing the right-hand part to one mesh by combining the capacitances C and C' (ignoring the latter) and combining the inductances L' and L.

The procedure just described reduces the network of Figure 1d to the form of Figure 9 of the paper. An alternative method of simplifying the network is to ignore the small capacitances $2(C''+C')$ in comparison with the adjacent large capacitances $2C$ and $3C'''$ and combining the inductances $^1/_2L'$ and $^1/_2L''$. If the inductances in the two right-hand meshes of Figure 1d are relatively large in comparison with the remaining inductances, it would be desirable to use a two-mesh solution for this part of the circuit instead of the single-mesh solution used in the paper.

In setting up equivalent networks for the solution of transient problems, it is preferable to start with accurate network representation for the various circuit elements and then with the network in view to simplify it by the usual methods, such as combining branches in series and in parallel or eliminating unimportant branches. This procedure is preferable to one in which certain branches are eliminated and other branches combined before the network has been set up.

Reference

1. System Recovery Voltage Determination by Analytical and A-C Calculating-Board Methods, R. D. Evans, A. C. Monteith. AIEE Transactions, volume 56, 1937, June section, pages 695–705.

C. Concordia and H. A. Peterson: For discussion, see page 1015.

Raymond C. R. Schulze (Public Service Electric and Gas Company, Newark, N. J.): This paper is very interesting to me, since I was one of those people involved in the recovery voltage rate survey. When the survey was started, the working group was called together, and Mr. Adams outlined the method of calculation. The procedure is clear and easy to follow, except that there appear to be certain disadvantages to the application of Boehne's method for the calculation of the various natural frequencies. This method requires the combination of certain inductances and capacitances in order to reduce the number of oscillating circuits, and then it requires the use of certain very complex graphs which appear as Figures 20 and 21 of the paper.

The objection to the method is based on these thoughts:

1. The combination of certain inductances and capacitances results in a circuit with a natural frequency, which does not actually exist in the system; certain actual frequencies, therefore, do not appear in the calculations. This may result in an erroneous value of recovery voltage, and recovery voltage rate. Also, when combining inductances, one must be careful to maintain the true series circuit of inductances (reactances) from the generators to the fault, in order to maintain the proper voltages across each reactance of the system.

2. The graphs of Figures 20 and 21 are quite complex, and one must have blind faith in the accuracy of the graphs and in the factors which one reads from these graphs.

3. Furthermore, there is no way to check one's work when using this method, except to repeat the whole process (and perhaps the same mistakes).

Therefore, the method used in Public Service Electric and Gas Company was to continue with the diagram of Figure 6 and to resolve it into its oscillating circuits without any reduction in the number of circuits. This meant that there were as many oscillating circuits in the final diagram as there were in the original system diagram. This meant, in turn, that each natural frequency in the system was represented in the calculation. Trouble may be experienced because of a change in the natural frequency of a circuit, when the natural frequencies of two adjoining circuits have a ratio of less than two to one, but in these cases the correct natural frequencies can be obtained either by using Boehne's graphs for these particular cases (of which there were very few) or by computing these natural frequencies by the usual methods employed for coupled circuits. An excellent reference for a study of the behavior of oscillating circuits is the Bureau of Standards circular 74.

The method utilized in Public Service Electric and Gas Company should be especially useful after reading the conclusions shown in Mr. Van Sickle's paper.[1] Mr. Van Sickle shows that the various natural frequencies die out at varying rates, so that a truer calculation should be possible when each frequency is identified.

Also, the calculations seemed to indicate that the highest value of recovery rate was for a fault just beyond a feeder reactor, and this recovery rate was largely dependent on the natural frequency associated with this reactor. On this basis an equation of a half dozen terms was developed, which gave answers to within five per cent of those obtained by the long detailed process for faults just beyond the feeder reactor.

As mentioned before, Mr. Van Sickle's work shows that the various frequencies die out at different rates. This indicates that the one uncertain factor in the method of calculation is the decrement factor to be used for each of the various natural frequencies. The accuracy of the calculation of recovery voltage could be improved, if a graph of natural frequency versus decrement factor could be obtained.

REFERENCE

1. TRANSIENT RECOVERY VOLTAGES AND CIRCUIT-BREAKER PERFORMANCE, R. C. Van Sickle. AIEE TRANSACTIONS, volume 61, 1942, November section, pages 804–13.

R. L. Webb: For discussion, see page 1014.

J. B. MacNeill: For discussion, see page 1015.

A. J. Krupy: For discussion, see page 1015.

J. A. Adams, W. F. Skeats, R. C. Van Sickle, and T. G. A. Sillers: The analyzer referred to by Mr. Dunlap should prove to be very helpful in analyses of this kind and would have been used more extensively in the Association of Edison Illuminating Companies survey, had it been available. It was used in at least one case to check part of a circuit which had been calculated, and the results were astonishingly close to the calculated values. However, other circuits which are more complex would probably show wider discrepancies, and in these cases the analyzer should be most helpful. It is usually not possible, however, to obtain the complete recovery voltage characteristic with an analyzer, as this would mean isolating a large portion of the power system, which is generally not permissible.

With respect to the accuracy of the procedure, it was pointed out in the paper that the recovery rate for breakers having the same voltage and current rating may vary over a range of almost 100 to 1 and, while quite different performance may be expected in some cases at opposite ends of this range, it is seldom that an important difference in performance will result from a change of even a good many per cent. The accuracy required in these calculations, therefore, is not of a very high order, and it is quite permissible to sacrifice accuracy to simplicity to a considerably greater extent than could be tolerated in many other fields. This has been done in many instances throughout the paper, as it was quite necessary in order to keep the job within the realm of economic possibility.

Mr. Evans' discussion echoes a warning also found in the paper to the effect that caution must be exercised in applying the methods of the paper where a high degree of accuracy is required.

With further reference to Mr. Evans' discussion, it may be pointed out that the phase-sequence circuit reduces at an early stage of simplification to the same thing as the circuit used by the authors, as may be seen by comparing Mr. Evans' Figure 1c with the authors' Figure 6. The high desirability of this simplification where accuracy requirements permit is indicated by the difference in complexity between Mr. Evans' Figure 1c and his Figure 1b.

The authors agree that it is preferable to start with accurate circuit representation and recommend that this be done to the extent compatible with the complexity of the job. A great many of the simplifying steps will depend upon the relative magnitude of the capacitances and inductances involved. This was the case with the steps between Figure 6 and Figure 8 of the paper, which, as Mr. Evans suggests, would not have been sound under different conditions.

No hard and fast rules can be set up for the simplification procedure, and a certain amount of judgment is necessary in deciding just what steps to take. This is illustrated by Mr. Schulze's discussion which points out that he made rather wide departures. It is probable that the inclusion of all circuits resulted in improved accuracy in some cases, while his distrust of the curves caused some loss in others.

The factors mentioned by Concordia and Peterson should of course be recognized in applying and designing circuit breakers. This paper covers only the recovery voltage part of the picture and makes no attempt to review these other factors.

A Compressed-Air Operating Mechanism for Oil Circuit Breakers

Discussion and author's closure of paper 42-114 by R. C. Cunningham and A. W. Hill, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, September section, pages 695–8.

H. E. Strang (General Electric Company, Philadelphia, Pa.): Development during the last few years on air-blast circuit breakers, in which contact movement is provided by pneumatic mechanisms, has built up a better understanding of the simplicity, sturdiness, and reliability of this type of operating power. It is quite natural that this has led to the development of such air mechanisms for the operation of oil breakers. Similar to others which have been described, the mechanism disclosed by Cunningham and Hill undoubtedly possesses many of these desirable characteristics.

Without belittling in any way the importance of this new type of operating mechanism, it must not be inferred that it alone made it possible to provide the industry with super-speed reclosing circuit breakers. The first mechanisms for such service, which were of the motor-operated type, were installed on 138-kv breakers in 1936 and have been in regular service ever since. Subsequently, over 50 more of this motor-driven cam type have been placed in service, and, as shown by reports to the Institute, have contributed their share in demonstrating the value of this kind of triple-pole, super-speed reclosing service on high-voltage lines.

Since super-speed reclosing of triple-pole high-voltage breakers was already an established procedure, it was only natural that pneumatic operators should be employed for this service. It has now been established that they perform this same function with ease and with the obvious advantages incident to stored energy and low battery drain. Starting with the first installation during the summer of 1940, well over 100 pneumatic mechanisms have already been placed in service on both new and modernized oil circuit breakers of all voltages from 34.5 kv to 230 kv.

In attempting to reconcile the relatively flat output characteristics of the simple air piston with the rising characteristics of force required to close a circuit breaker, Cunningham and Hill have resorted to a mechanically operated throttling valve which opens near midstroke to give the piston an additional boost. Under ideal conditions this should work satisfactorily, but at best it is another moving part which may require some adjustment, maintenance, and attention. A simple toggle built right into the mechanism between the driving piston and the output crank has been found by experience to serve as the means of matching these characteristics, and eliminates the need for throttling valves or other such critical moving parts.

H. W. Haberl (Montreal Light, Heat, and Power Co. Consolidated, Montreal, Que., Canada): If the control mechanism for oil circuit breakers as described in the above paper is to be used in freezing temperatures where large variations above and below freezing occur, it is suggested:

(*a*). That the inlet air to the compressor be passed over by some type of drying equipment; this could be in the form of a glass bottle in which colored activated alumina or silica gel were used as the dehydrated medium; in this way, the air in the storage tank would be reasonably dry.

(*b*). The mechanism's housing be lined with an insulating material, similar to (ten-test) and a thermostat and heater mounted inside the control cabinet wnich would maintain the control cabinet at or above freezing temperature.

The preceding suggestions would insure the air being free of moisture and consequently would prevent any of the small valves from freezing.

R. C. Cunningham and **A. W. Hill:** Mr. Strang has pointed out the relative effect of a throttle valve and a toggle, either of which modifies the flat characteristic of the simple air cylinder to match the typical breaker load. One reason for including a simple mechanically operated throttle valve is to make possible the application of this mechanism to existing breakers. In this way, the pull curve is made to follow closely that of the solenoid formerly used.

Another and more important reason for using the throttle valve with a slotted link is to provide controlled flow of air on ordinary closing, and full air flow when needed for rapid reclosure. A range of adjustment, both as to time of operation and amount of opening of the throttle valve, makes it suitable for use on any of several sizes of breaker.

It is obvious also that with the breaker starting from the full open-contact position, air is admitted above the piston when the cylinder volume is smallest, and full air pressure immediately acts to start movement. A restriction in the air line is permissible and desirable to prevent excessive speed later in the closing stroke. However, for fast reclosure, air is admitted while the piston is part way out in its stroke, so that the full effect of the supply pressure is not felt until after a time interval while air is being poured into this large cylinder volume. A large opening in the air line serves to gain time, so that the utmost over-all reclosing time can be secured.

The use of a drying agent, as suggested by Mr. Haberl, was considered in the early design stages but not adopted, as it would introduce another item requiring maintenance and possibly be the cause of trouble if essential to proper operation and not properly cared for. During freezing temperatures, there is so little water in the air supplied to the compressor that none will be deposited after the air passes the inlet valve. Heaters are supplied to maintain all parts within the housing at a temperature enough above the outside ambient to prevent trouble, although in extreme low outside temperatures it is not considered necessary to keep the parts above the freezing point.

In a test set up at the factory one of these mechanisms was operated at temperatures down to −20 degrees Fahrenheit. Even when air was drawn from outside the cold room, at a temperatnre of 50 to 60 degrees Fahrenheit, freezing of condensed moisture gave no trouble at any of the control valves. Similar performance is indicated by breakers in regular winter service.

Tests and Analysis of Circuit-Breaker Performance When Switching Large Capacitor Banks

Discussion and authors' closure of paper 42-119 by T. W. Schroeder, E. W. Boehne, and J. W. Butler, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, November section, pages 821–31.

R. M. Partington and **M. E. Scoville** (General Electric Company, Pittsfield, Mass.): The physical arrangement of a capacitor bank and circuit connections determines the inductance and, therefore, affects the magnitude of inrush currents with energizing the bank. Maximum inrush currents are obtained with switching equipments in compact physical arrangements. It may be of interest to compare the inrush currents calculated from physical dimensions to the results of the actual tests made by the authors. Figure 1 shows a compact equipment rated 2,520 kva. A number of such equipments may be grouped together with space to allow for ventilation and accessibility to form a larger bank. One or more of those equipments may be connected to the system through a suitable circuit breaker.

Assuming a typical arrangement for each of the three-phase kilovolt-ampere ratings, wye-connected, inductance and resistance values have been calculated, conservatively leaning toward the minimum practical values. The circuit used for calculation includes individual capacitor units, fuses, bus connections, cable between equipments, and connections up to line side of circuit breakers. The inrush currents were calculated from the relation

$$i\,(\text{maximum}) = \frac{\text{line volts} \times 1.41}{1.73} \times \sqrt{\frac{C}{L}}$$

neglecting resistance which has a negligible effect on the inrush current magnitude.

The inrush currents shown in the table are maximum with assumption of infinite bus at line side of circuit breaker terminals. Consequently the presence of other capacitor banks on the same infinite bus would not increase the values of maximum inrush current. Rarely, however, would these inrush currents be obtained in practice since there would be some system inductance ahead of the circuit breaker which would reduce the inrush current?

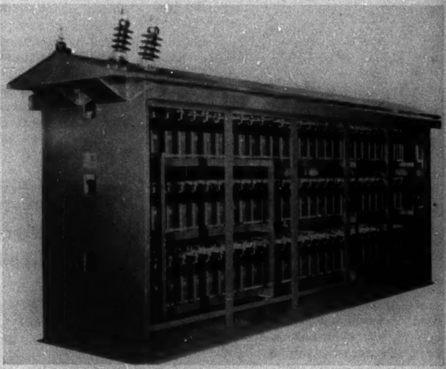


Figure 1. Outdoor large rack-type capacitor equipment, rated 2,520 kva, 13,800 volts, wye-ungrounded neutral, three-phase, 60 cycles

Table I

Rated 60-Cycle 3-Phase Line		Calculated Values Per Phase			Maximum Inrush Current (Amperes)
Kva	Voltage	Inductance (Microhenrys)	Capacitance (Microfarads)	Resistance (Ohms)	
2,520	4,160	8.17	387	0.0025	23,400
5,040	4,160	4.08	774	0.0012	46,700
10,080	4,160	3.05	1,548	0.0006	76,500
2,520	13,800	9.15	35.2	0.0046	22,100
5,040	13,800	4.46	70.4	0.0023	44,300
10,080	13,800	3.34	140.8	0.0012	73,000
5,400	25,000	11.27	23.0	0.0042	29,100
10,800	25,000	8.45	46.0	0.0022	47,600

Using the inductance and resistance values from the table, it can be shown that the transient inrush current will decrease to a negligible value in less than one cycle (60-cycle frequency). For example, consider the 10,080-kva bank on a 13,800-volt 60-cycle line. The transient current will decrease to one-half its maximum value in approximately one-quarter cycle (0.00416 second), or to one-tenth its maximum value in approximately three-fourths cycle (0.0125 second). The natural period of the transient would be approximately 7,300 cycles per second.

The above calculated data indicate maximum inrush currents of 76,500 peak amperes with a physical arrangement typical of a practical permanent installation except that an infinite bus is assumed at the circuit-breaker terminals. Actually, the additional inductance in 30 feet of line between the circuit-breaker terminals and an infinite bus, which is typical of a practical installation, will limit the peak currents to less than the values obtained in the tests recorded in the paper.

R. D. Evans (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The transient-recovery-voltage problem for de-energizing capacitors is similar to that which arises from de-energizing transmission lines. The latter problem was investigated several years ago in the paper, "Power-System Transients Caused by Switching and Faults" by Evans, Monteith, and Witzke.[1] It is pertinent to compare the results of these two studies since part of the analytical and transient-calculator work of the present paper follows closely the procedure used in the earlier paper. In that paper a power system with typical sending and receiving stations and with two 100-mile transmission lines were used for the study of several types of switching transients. The case considered included the de-energizing of one line starting with one and two lines in service. The comparison of the recovery voltages obtained is given in the accompanying Table II.

The difference in voltage transients incident to de-energizing transmission lines between switching lines and de-energizing capacitors depends to some extent on the differences between lumped and distributed capacitances and the presence of line resistance. However, the more significant difference for many conditions of operation arises from the difference in the positive- and zero-sequence constants. Capacitor connections may be made so as to give widely different ratios of zero- to positive-sequence constants, whereas with transmission lines the ratio is fixed and for the case covered by the earlier paper was in the ratio of 1 to 1.35. This means that the voltage transients on grounded capacitors with neutrals grounded or interconnected should correspond approximately with the transients of switching lines connected to grounded systems.

It is believed that the differences in the results of tests for the comparable conditions from the two papers may be explained on the differences in the circuit constants.

Table II

	Transient Voltages for De-energizing Operations Expressed as Ratio to Rms Voltage Line to Ground			
	Voltage to Ground		Switch Volts	
Switching Conditions	Transmission Line*	Capacitor**	Transmission Line*	Capacitor**
Grounded neutral				
Transmission line				
Initially 1	2.7		3.6	
Initially 2	2.3		3.4	
Capacitor				
0.5 microfarad		3.0		4.0
0.5 microfarad with 0.2 microfarad unswitched		2.4		4.8
Interconnected capacitor neutral				
0.5 microfarad with 0.2 microfarad unswitched		3.3		4.3
Ungrounded neutral				
Transmission line				
Initially 2	2.6		5.0	
Initially 1	2.2		3.8	
Capacitor				
0.5 microfarad		6.0		6.6
0.5 microfarad with 0.2 microfarad unswitched		6.0		8.2

* Reference 1.

** From the authors' paper.

The earlier paper also gave data on voltage transients for two restrikes. Incidentally, it may be observed that these tests showed lower transient voltages for some value of neutral impedance than with solidly grounded systems.

The results obtained by the authors of the present paper are of interest and show the desirability of reducing transient voltages by grounding capacitor neutrals or by interconnecting them. There is also some advantage in switching capacitors in relatively small blocks.

REFERENCE

1. POWER-SYSTEM TRANSIENTS CAUSED BY SWITCHING AND FAULTS, R. D. Evans, A. C. Monteith, R. L. Witzke. AIEE TRANSACTIONS, volume 58, 1939, August section, pages 386–97.

T. W. Schroeder, E. W. Boehne, and **J. W. Butler:** Partington and Scoville have provided some very useful data for estimating inrush currents and their duration for typical large banks. It should be emphasized that the inrush or discharge currents will be reduced measurably by comparatively short lengths of conductor to the capacitor banks. It is felt that the maximum test value of 52,000 amperes is sufficient for most practical installations because

1. This is a greater equalizing current than that which will flow between two groups of two 10,080-kva 13.8-kv banks each, with an inductance of five microhenries between the groups. This value is about 44,600 amperes.

2. A bank of 10,080 kva which is shown to have higher inrush current than values reached in test (76,500 amperes maximum) could discharge such current through a breaker only in the case of a solid three-phase fault immediately on the line side of the breaker, which would be a rarity.

3. On energizing such a bank, very small values of system inductance will greatly reduce the possible inrush current.

120-Kv Compression-Type Cable

(42-133)

120-Kv High-Pressure Gas-Filled Cable

(42-135)

Low-, Medium-, and High-Pressure Gas-Filled Cable

(42-102)

Discussion and authors' closures of papers 42-133 and 42-135 by I. T. Faucett, L. I. Komives, H. W. Collins, and R. W. Atkinson, and paper 42-102 by G. B. Shanklin, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, September section, pages 652–7, 658–65, and October section, pages 719–26.

L. L. Phillips (General Electric Company, Pittsfield, Mass.): Some of the all-gas-filled terminals to which Mr. Shanklin refers now are in service at 15 kv. Similarly designed terminals for ratings 23 kv, 34.5 kv, and 46 kv, have been developed and are available for gas-filled cable installations pending. These gas-filled terminals utilize an ingenious method for controlling the electric stresses in the insulation at the termination of the cable-shield extension. The ordinary construction of termination regularly used in compound-filled and oil-filled terminals is unsatisfactory for gas-filled assemblies, because of the differences of voltage distribution resulting from the use of gas as a part of the dielectric.

Stress control is accomplished largely by a specially developed cloth having the capacity to divert a part of the charging current which tends to flow directly to the end of the cable-shield extension. The cloth is processed to have a controlled resistance to the flow of current. It is assembled in contact with the metal of the cable-shield extension to provide a partially conducting cylinder for approximately one inch beyond. The special cloth serves to distribute the charging current through a larger cross section of the insulation, thus preventing concentration at the immediate end of the cable-shield extension. Therefore, a part of the charging current flows from the terminal conductor through the insulation to the cylindrical surface formed by the applied cloth and thence to the cable-shield extension.

In addition to the "shielded" extension for stress control, wrappings of varnished cambric tape are applied to exclude filler gas—except in the thin films between tapes—from regions immediately surrounding the cylinder of special cloth. The total thickness of this applied tape varies according to the circuit voltage at which terminals will operate in service. Thus, any appreciable thickness of the filler gas is prevented from entering the dielectric circuit, except at suitable locations.

L. I. Komives (The Detroit Edison Company, Detroit, Mich.): After seeing with his own eyes 29 inches of vacuum in an impregnated-paper cable line which operated at 24 kv successfully for the last 12 years in Detroit, the writer appreciates Mr. Shanklin's contribution to the industry more than ever before. If some means could be found which would enable the cable user to maintain positive (above atmospheric) pressure at all times in a solid-type cable, certainly more efficient operation could be expected than from the above described Detroit cable. As the service record of the solid-type cable is very good, the insulation thickness may be reduced, or the normal maximum operating temperature (cable loading) may be increased. This is essentially what the low-gas-pressure cable accomplished in addition to the "self-supervision," a feature which is very valuable to the operating engineer.

In this paper Mr. Shanklin takes the apparently very logical step and tries to extend the usefulness of this principle into higher brackets, in regards to both pressure and voltage. He divides the field into three pressure classes and prescribes cables suited for each class. The writer will discuss each class separately.

1. LOW-PRESSURE GAS-FILLED CABLE

There is only one observation which the writer would like to make with regard to this cable. Nowhere in this nor in Mr. Shanklin's previous AIEE paper describing

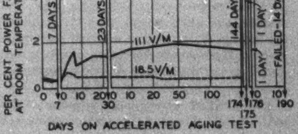


Figure 1

this cable has a direct parallel been drawn nor comparative test results shown which would indicate the superiority of the low-gas-pressure cable over that of an ordinary solid cable. It would be desirable to show for instance paralleling cases for Figures 6, 10, 11, and 12 of Mr. Shanklin's paper where no nitrogen is applied to ordinary solid cables employing the same dimensions and construction as the low-gas-pressure cables shown in Figure 1 of this discussion.

The argument that the low-gas-pressure cable of Figure 6 stood up for more than a year under certain conditions would be more convincing if parallel cases on solid-type cables would be shown. In Detroit solid-type cables stood tests at higher voltages than that shown on Figure 6. One such example is shown on Figure 1 of this discussion.

2. Medium-Pressure Gas-Filled Cable

The economic voltage range for this cable is given as from 40 kv to 69 kv. This is the range which is the most awkward and bothersome from the cable designers' point of view—apparently too high for solid-type insulation and too low to permit the application of reservoirs or other accessories economically. If the medium-pressure system will solve some of the problems in this field, it will be a real contribution. In the opinion of the writer the problem of the high-creep-strength alloy sheath will have to be worked out more completely especially with regard to higher temperatures, resistance to corrosion, and aging. The double-reinforced sheath could be justified in very few cases and under a constant pressure of 30 pounds per square inch. This construction still has to prove itself.

3. High-Pressure Gas-Filled Cable

In paragraph 2 of this discussion the statement was made to the effect that Mr. Shanklin took the "apparently" logical step of extending the gas-feeding principle into the higher-voltage field. As long as Mr. Shanklin tries to improve the solid-type cable, he will find the majority of the cable engineers behind him, because most of them realize the inherent weaknesses. In the higher-voltage field, however, there are several excellent solutions, and naturally these systems should be used as a yardstick when judging Mr. Shanklin's high-pressure cable.

A. Sheaths intact:

1. Compression versus high-pressure: Theoretically the lead sheath is not flexed in the high-pressure system, but it is flexed in the compression system. On the other hand the gas in the pressure chamber is in contact with the insulation in the high-pressure cable from the very beginning, while in the compression system the same conditions will appear only if and when the lead sheath ceases to function as an impervious membrane. Otherwise, the two systems are alike except that the cable of the high-pressure system will cost somewhat more if three-conductor cable can be used, and a great deal more if single-conductor construction has to be resorted to.

2. Oil-filled versus high-pressure: The writer would like to see a comparison in cost between these two types of cable. As the spread between oil-filled and compression cables is not too great, the higher cable cost of the high-pressure system would probably bring it quite close to the cost of the oil-filled system. In such a case the new construction would have to compete with the reliable proved oil-filled system on purely technical basis.

B. Sheaths stripped off:

1. Modified Beaver (*SMD*) versus high-pressure: When Mr. Shanklin strips his sheath, his cable in time becomes an *SMD* cable with the following exceptions:

(*a*). He does not design his cable for such conditions, that is, grades his butt space thicknesses, insures gas supply to all voids, and so forth.

(*b*). He has to use an impregnating compound of lower melting point; otherwise he cannot impregnate. With lower melting point oil drains at corresponding temperatures, and in a large steel pipe there is nothing to hold the oil in the cable insulation. In an *SMD* cable impregnating compound with a much higher melting point may be used.

(*c*). He does not even use strand shielding, because tests on "new" cables do not show the need of it.

(*d*). His cable cost probably somewhat less, because he does not use preimpregnated tape in a low-humidity room. Although in order to obtain good taping, he probably "regulates" the humidity of the taping room.

Between the two alternatives the writer prefers the *SMD*.

Strand Shielding

Although strand shielding did not appear to show any improvement in the long-time tests, it should still be retained, because it influences impulse strength. It is a well-known fact that insulation thicknesses of voltage cables are based on transient voltage requirements. This is emphasized in Mr. Shanklin's paper.

Impulse Tests

The writer would like to add a note of warning to Mr. Shanklin's conclusions. It has been fairly well established that aging does not affect the impulse strength of an oil-filled cable, but it does affect that of the solid-type cable. As the gas-pressure cable constructionally speaking is somewhere between the two, it seems that 21 days aging at 75 degrees centigrade should not be accepted as a complete proof that aging does not affect the impulse strength of the gas-pressure cable.

L. F. Hickernell (Anaconda Wire and Cable Company, Hastings-on-Hudson, N. Y.): Referring to Figure 6 of Mr. Shanklin's paper, it is noted that after 252 days at 85 volts per mil the power factor at 85 degrees centigrade was two per cent. Reference to a published AIEE paper ("The Type-*CB* Impregnated-Paper Cable," by S. J. Rosch, Figure 12), will show that after a similar period at 100 volts per mil, the power factor of type-*CB* cable at 80 degrees centigrade was of the same order (that is, two per cent). From this point on the test conditions are not comparable. Accordingly, from a power-factor-stability standpoint, it would not appear that the added complication of gas-pressure operation were warranted.

Referring to "Strand Shielding," comparison of Figure 6 with Figure 10, insofar as it goes, substantiates the author's conclusion that no great benefit was obtained from stand shielding. Figure 10, however, at the time of writing the paper was incomplete, the cable having been subjected only to the 85-volts per-mil cycles. Since the cable on test was constructed with metallized-paper strand shielding, we presume the author intends to limit his conclusions to this design only.

In reference to "Impulse Tests," it is stated that for all practical purposes, solid, oil-filled, and gas-filled cable have the same impulse strength; this appears reasonable. However, in this connection, consideration should be given to the fact that other investigators have found strand shielding to improve the impulse strength ("Impulse Strength as a Measure of Cable Quality," by L. I. Komives,[2] and "Impulse Strength of 24-Kv Type-*H* Cable," by G. B. McCabe, transmission and distribution committee, Edison Electric Institute, Detroit, Mich., February 17, 1941).

The type *SMD* cable discussed in paper 42-135 is typically American in that it represents a melting pot of ideas. Taking the graduated gas spaces of Beaver, the designers have put the cable in Hochstadter's steel pipe, thrown away the lead-sheath diaphragm, and applied the pressure in direct contact with the impregnated insulation as suggested by Fisher, Atkinson, Bennett, and probably others.

All that is needed to make this a truly up-to-date design would be to incorporate Rosch's carbon-black protection. On this point, however, the designers have apparently been content on simple conductor shielding. Since impulse strength was evidently of some concern (refer to "Impulse Tests"), this seems unfortunate, in view of one of the authors' own data[2] and that of one of his associates ("Impulse Strength of 24-Kv Type-*H* Cable," by G. B. McCabe). These data indicate the highest impulse strength was obtained with type-*CB* construction.

Referring to "Field Tests," in discussing Figure 2, the authors state: "These measurements show no evidence of deterioration of the insulation." So far as it goes, this statement is correct, but it might be added that the data do not show anything else either; certainly not that no deterioration took place. Power-factor measurements at (or below) *room* temperature are valueless as criteria of insulation deterioration. Reference to cyclic-aging data will disclose that during the cycles prior to instability the cold power factor seldom exceeds appreciably the initial power factor. On the other hand, the hot power factor gradually increases until failure (reference 1, Figure 12). It is the high-temperature, not room-temperature, power factor which discloses the presence of deleterious products which cause insulation deterioration.

Referring to "Compound Migration," if a 65-day test at 100 degrees centigrade can be relied upon to indicate the expected life of the cable at rated operating temperature, it would appear that the migration problem has been solved by the use of a high-viscosity impregnant (3,000 Saybolt Universal seconds at 100 degrees centigrade). On the other hand, operating experience on this same system has indicated that viscosity is not the only factor involved in migration. ("Examination of 60 Paper-Lead Cables After Operating in High Vertical Runs," by F. Farmer, underground systems committee, National Electric Light Association, Detroit, Mich., April 4-5, 1933). It will, accordingly, be interesting to learn at a later date the actual field experience with respect to compound migration. Until such time it might be well to accept summary 3, and possibly 4, with some qualifications. This observation is further prompted by the fact that tests in several laboratories and operating experience have indicated that migration will *eventually* take place, no matter how high the viscosity, if the impregnant is operated above its melt point.

I like one important feature of this system, modestly omitted by the authors, namely, that it has a second line of defense. It it doesn't work with gas, it can always be filled with oil; provided rationing has not extended to the Midwest by that time!

REFERENCES

1. THE TYPE-*CB* IMPREGNATED-PAPER CABLE, S. J. Rosch. AIEE TRANSACTIONS, volume 59, 1940, page 1047.

2. IMPULSE STRENGTH AS A MEASURE OF CABLE QUALITY, L. I. Komives. AIEE TRANSACTIONS, volume 60, 1941, October section, page 929.

Robert J. Wiseman (The Okonite-Callender Cable Company, Inc., Passaic, N. J.): I am not able to go along with Mr. Shanklin's statement that a pipe cable system is limited in open ground and not adaptable to paved city streets. I think the Detroit Edison installation of the 120-kv gas-pressure cable proves this, and calculations that we have made on installation costs indicate that our Oilostatic cable system can compete very nicely with a cable system drawn into ducts. It is true that it is necessary to draw the three cables into one pipe and, therefore, the heat is concentrated more than for a duct system, but the heat-dissipating properties of a buried cable or pipe are better than a duct, and, therefore, we arrive at the same conductor size or in some cases a smaller size. A steel-pipe cable system (Oilostatic and gas-pressure) is able to carry as heavy loads as is considered good engineering in concentrating in one circuit large amounts of power. The Oilostatic system has a very high emergency-overload characteristic because of the high heat storage capacity of the oil in the pipe and the surrounding earth, so that, if occasion arises, it can be heavily overloaded for several hours without reaching excessive temperatures, and it will cool rapidly when the load is reduced.

I do not agree with the statement in paragraph *d* referring to the effect of exposure of the unleaded cable as it is being drawn into the pipe to absorption of air and moisture, as it pertains to the Oilostatic system, and I also question it for the gas-pressure system. This is another of the fears that we had in the past that is not borne out by facts. We have drawn the individual unleaded cables for the Oilostatic system into pipes for lengths of 1,500 to 4,000 feet, and power-factor measurements did not show any increase. Many of us have stripped lead sheaths off cables, left them exposed, and then measured the power factor without finding an increase over the values before removing the lead sheath.

I am disappointed that the authors of paper 42-133 have not included in the paper any data regarding the thermal characteristics of the compression-cable system. They refer to load-cycle tests that have been made but do not tell us what temperatures were found for the conductor, lead sheath, and pipe, nor show curves of the temperature rise with time when the load is on, and the rate of cooling when the load is off. A load cycle of 2.5 hours on and off and four hours on and off is a very short cycle, and I doubt that the cable arrived at its maximum temperature at all. The 190-hour load cycle should show good data. The heat-storage capacity of a buried-cable system is very large and results in a slow rate of rise of temperature. In the case of the compression cable you have copper, insulation, lead, gas, steel pipe, and surrounding earth. For the high-gas-pressure cable there is no lead. For the Oilostatic-cable system we have copper, insulation, oil, pipe, and surrounding earth. Tests which we have had made on Oilostatic cable indicate the heat capacity inside the pipe is very high and results in a slow rate of rise in temperature outside the pipe. One test indicated that the temperature of the earth 17 inches from the center of the pipe did not begin to rise until six hours after the load was applied to the cable. I would expect the compression cable to have good but not as much heat capacity because of the use of gas which has a very low heat capacity instead of oil surrounding the cables inside the pipe.

I am also interested in the thermal constants for the compression cable, insulation, gas zone, earth. What formulas are used for calculating the thermal gradient in each zone?

The *SMD*-type cable is a logical development from the Beaver-type cable as used in England and the compression cable; that is, take the pipe of the latter and pull into it preimpregnated paper-insulated cables and apply high gas pressure to fill all the spaces in the insulation with gas. It differs from the Oilostatic system, in that, the latter has oil as the pressure medium, and oil is forced into all the spaces in the insulation. As the oil is gas-free, when pumped into the pipe which has been previously evacuated, and fills all voids, there is no worry about ionization taking place, so that we do not have a critical electric stress at which ionization will start.

We do not consider it necessary to lay up the three conductors in the Oilostatic cable, because there is free movement in the pipe to take care of the longitudinal thermal expansion. The authors found this to be so for their cable. The testing of a 50-foot length for the end thrust as described in the compression-cable paper is inconclusive, as the length is too short as compared to actual installation. Actually we have at least several hundred feet between joints, and on installation we have noted a gradual turning as the cables are drawn in, giving the equivalent of a long lay cable which will result in radial expansion of the three conductors instead of longitudinal, as they are not bound together, and so are free to move radially.

My comments on lack of thermal data in the compression-cable paper also apply here. The authors stated that they have collected a great deal of data; yet they do not give us this necessary information to understand properly how to design this system. We are interested in the temperatures found for the conductor, shielding tape over the insulation and pipe. We would also like to see curves showing the temperature rise with time while the load is on and the rate of cooling when the load is dropped. I question very much the value of such short load cycles as 2.5 hours on and off, and four hours on and off. I doubt that the cable will arrive at its maximum temperature at all. Information on the 190-hour load-cycle test would be helpful in appreciating what this system is capable of doing in the way of taking care of heavy loads. We will be interested to compare the heat-storage capacity of this cable with the heat-storage capacity of the compression and an Oilostatic cable.

The information regarding soil thermal resistivity is very interesting. I am glad that the authors state the formula in the third paragraph is empirical. We have used a sphere in making thermal-resistivity tests and the well-known sphere formula. The values of soil resistivity given in Figure 4 are quite interesting and show fairly low values. However, the important thing is how do they use this thermal resistivity in determining the thermal resistance of the earth term for the total thermal resistance of the system?

The coefficient of friction of 0.37 for pulling the cables into the pipe up to 1,500 feet is very good considering it is a dry pipe. We have found for the Oilostatic system for lengths of 2,000 feet and more a value of 0.26. We have no hesitancy in pulling as high as 4,000 feet of cable without excessive strains on the conductors.

E. H. Kirkham (Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): Under some circumstances it is desirable that a pressure relay be installed at the far end of a gas-filled cable when gas feeding from one end only. In the event that the feeder is operating as a closed system this relay will function equally well at either end. However, should it be necessary to operate with a constant supply of gas from a cylinder through a regulator, while the regulator will maintain a relatively steady pressure at the feeding end, pressure gradient in the cable may be such as to result in a dangerously low pressure at the far end. In one instance, this occurred with a leak in the order of one-half cubic foot per minute when fed at 12 pounds per square inch from one end; the pressure at the far end was measured at six pounds per square inch. Of course, the temporary feed was supplied at the far end pending location of the leak.

I agree with Mr. Shanklin that it is not necessary to "blow out" gas-filled cable equipped with a solid tube, but experience has shown that the excess compound should be drained from the joints periodically, a procedure which has the same effect as blowing out the cable.

At the present time, field experience indicates that this need not be done oftener than once a year, and probably, as the free compound is eliminated from the cable, the period may be extended.

E. R. Thomas (Consolidated Edison Company of New York, Inc., New York, N. Y.): There has been another type of compression cable described before the Institute which was not mentioned in paper 42-133. This was my paper on sheath-compensated cable presented before the winter convention several years ago. The cable described at that time has operated satisfactory at pressures up to 100 pounds per square inch.

From an operating viewpoint several points in the compression-cable paper are of interest. First, the increase of several hundred per cent in the life of the lead sheath, when subjected to diaphragm action, by the elimination of oxygen from contact with the sheath surface, seems to be of practical value. We have occasions in New York to run cables over bridges and other vibrating structures and would like to inquire whether the fatigue strength would be similarly increased under these conditions by the elimination of oxygen from contact with the sheath surface?

Frequently cracked sheaths occur, particularly in the smaller manholes, from longitudinal movement and concentrated bending. Would it not be possible to reduce the cable fault rate from this cause by sealing the sheath surface from oxidation at these vulnerable points?

It was also pointed out in this paper that longitudinal movement was negligible when single-conductor cables were cabled together with a short lay and no external covering used. Under certain conditions such a procedure applied to duct installations may provide a simple solution to the problem of longitudinal movement and its effect on the lead sheath.

The Consolidated Edison Company of New York has made a number of laboratory and field tests on three-conductor cables of special construction for determining the ability of such cables to absorb within the sheath the longitudinal expansion of the conductors. Cables made with short lays, special binder construction, and sheaths of different degrees of looseness were tested. Laboratory tests showed that the greatest gain in reducing the restraining force necessary to prevent longitudinal movement was obtained by using short lays. Field measurements on a number of cable sections having special binder construction and short lays showed cable movements on these sections to be considerably smaller than for equivalent sections of standard construction.

There are several items in the paper on compression cable that the authors may clarify by answering the following questions:

1. How are countersunk type of connectors applied to a cable joint?

2. In Figure 5 of the paper, is the step on which the joint sleeve ends the lead sheath of the cable? If it is, what is the next higher step on the cable?

3. Since a void is left at the end of the cable sheath in the joint, is there any tendency for the joint sleeve to buckle and put undue strain on the wipe when external pressure is applied?

4. What is the 60-cycle dielectric strength of the cable, as compared to that of the joint?

From the standpoint of simplification, high-pressure gas-filled cable seems to approach more nearly the dream of the operating man than any cable available for such high voltages. The test data are very illuminating and convincing. If the future operating record proves as highly satisfactory as is indicated to date, I think high-pressure gas-filled cable will be hard to compete with.

Of particular interest is the fact that pipe cable was well adapted for installation under city streets in a city of the size of Detroit. Under such conditions of installation, I should like to ask how the cost of such a pipe system compares with the conventional duct system and also, how much of this seven-mile pipe system was under paved city streets, as well as what type of street paving was used.

The measurements of soil thermal resistivity are not only interesting but very helpful because of the lack of exact knowledge on this subject. In many cases in this country 80 degrees centigrade per watt per centimeter cubed has been used as the net value for soil resistivity. The variation shown by the Detroit data, from 75 to 120, indicates that 80 is probably not a very conservative figure to use.

H. C. Frank (General Cable Corporation, Bayonne, N. J.): Some supplementary data in connection with the laboratory tests of diaphragm action of lead sheath, mentioned in the paper on 120-kv compression-type cable, are significant. The daily load cycle, it will be recalled, was simulated by a laboratory cycle of 4.8 minutes. Because of the shortness of the laboratory cycle, it was a real problem to insure that the lead sheath would move uniformly throughout its length.

It became apparent that very little volume change takes place until nearly the full-pressure differential is established. Arrangement was therefore made to establish substantially this full-pressure differential in the first one third of each half-cycle. Thus, two thirds of each half-cycle was utilized for producing volume changes in the cable at substantially full pressure.

Experiment showed that this arrangement resulted in uniform longitudinal distribution of diaphragm action, as evidenced by breaks and general damage to the sheath occurring no more frequently at the ends of the samples than elsewhere.

One projected application of the compression-type cable involved installation on a bridge carrying heavy traffic and under severe vibration. Experiments were made as to the feasibility of such an installation and the results are pertinent not merely to this type of cable but to installation of any ead-sheathed cable on a vibrating structure.

Samples of pure lead sheath, about four feet long, two inches in outside diameter, and having an 85-mil wall, were supported at both ends by trunnions and vibrated at their natural frequency (about 1,000 cycles per minute) by means of an electrically powered vibration motor. The amplitude of vibration could be regulated.

The modulus of elasticity, as calculated from the physical dimensions, mass, and experimentally determined frequencies, was found to be constant over a wide range of amplitudes (for the frequencies used in the tests) and to have a value of 1.7 to 1.9×10^6 pounds per square inch for common lead. The peak vibration stresses were readily calculated from this modulus, the physical dimensions, and the amplitude.

The curve showing cycles of life as a function of vibration stress was found to be asymptotic to a line parallel to the life axis. A sample will fail in a limited number of vibration cycles if stressed beyond this asymptotic value but will last indefinitely if the vibration stress is held below this value. For pure lead protected against oxidation the asymptote was found to be 500 pounds per square inch. When oxidation was not prevented, the value dropped to 350 pounds per square inch.

During these tests it was attempted to determine whether unbalanced stress would affect the fatigue limit. It was found that the fatigue limit was unaffected since no unbalanced force could be supported by the sheath when under considerable vibration stress. The sheath would creep rapidly until all such unbalanced forces or stresses were removed. It follows also from this that there can be no complications caused by residual stresses being superimposed upon vibration stresses.

G. D'Eustachio (General Cable Corporation, Bayonne, N. J.): Mention is made in the paper on 120-kv compression-type cable of the injurious effect of oxidation on diaphragm action. Specifically, the substitution of nitrogen for compressed air as the pressure medium in these tests resulted in increasing the life of laboratory samples by a factor of 4.

This effect of oxidation is in line with the generally known fact that corrosion of metals greatly hastens their destruction by fatigue. The following is offered as an explanation of the mechanism involved.

When a fresh surface of lead is exposed to air, a thin coating of oxide is quickly formed. Normally, this acts as a protection against further oxidation. However, in a sheath subjected to vibration or diaphragm action, the brittle oxide film is readily broken, and a fresh surface is continually presented to the effect of oxidation. Repeated bending tends to concentrate the action in definite cracks which ultimately penetrate the entire sheath.

William A. Del Mar (Phelps Dodge Copper Products Co., Yonkers, N. Y.): In the year 1935 I visited many of the principal cities of the United States and spoke before local engineering organizations on compression cable, exhibiting a motion picture of the then recently completed compression-cable system at Copenhagen in Denmark which I had seen under construction.

Engineers discussed this system at these meetings and the discussion was nearly always in one vein. They said, in effect:

"We have troubles on our cable systems and troubles on our high-pressure pipe systems. Now you describe a system that will combine these two kinds of trouble on one system. We prefer to take our troubles singly."

Ten years after the first European installation the Detroit Edison Company takes the plunge and decides to take on both kinds of trouble in their regular stride.

The type-*H* cable, also was held in abeyance except for one installation for about 10 years after its European introduction.

I have made my appreciative comments on the low-pressure gas-filled cable in the discussion of Mr. Shanklin's 1939 paper and will now confine myself to a few critical comments on some of the details.

Both Doctor Wiseman and Mr. Mildner in that same discussion, asked why Mr. Shanklin used average rather than maximum stresses in designing his insulation thicknesses.

I am one of those who believes that the maximum stress, as ordinarily calculated, is of doubtful significance in many cable problems where it has been used indiscriminately in the past, but it seems to me that the gas-pressure cable, at operating voltage, is one case where there can be little doubt of its applicability.

Mr. Shanklin seems to agree with this, for in his reply to Mr. Mildner he said:

> "Mr. Mildner is correct in stating that it is better to deal with maximum voltage stress than average voltage stress. Most of the cable lengths we tested had compact sector conductors with relatively sharp corners. For conductors of this shape it is difficult to express maximum voltage stress accurately, and it is for this reason that average voltage stress is generally used as a practical yardstick in the United States."

Thus, while Mr. Shanklin recognized in 1939 that the maximum stress should be used in the design of gas-pressure cables, and that this was not done only because of the difficulties, he continues to use the average stress for design purposes in his present paper.

Let us see where this leads us in a specific case.

Assume a 350,000-circular-mil 34,500-volt sector cable with the wall thickness recommended for this voltage, namely, 310 mils.

The average operating stress will be 64 volts per mil, and Mr. Shanklin considers this to be a satisfactory basis of design. We have studied the stress around the contour of a 350,000-circular-mil cable with 312 mils of insulation, using the electrolyte method, and have shown the results as a graph.[1] Reference to this graph will show that the stress at the surface of the conductor at the shoulders of the sector is 1.7 times the average stress, or 109 volts per mil, neglecting strand effect.

It is well known that ionization starts, in a drained cable, at a maximum stress of 50 volts per mil at atmospheric pressure. Mr. Shanklin's Figure 5 indicates that this is increased by a factor of 1.63 at 12 pounds gas pressure, that is, to 81 volts per mil.

We thus, have a cable operating at a stress of 109 volts per mil which is 35 per cent greater than its initial ionization stress of 81 volts per mil. It therefore appears that the low-pressure gas-filled cable takes about 70 per cent advantage of the 50 per cent increase in stress permitted by the self-extinguishing effect, and therefore Mr. Shanklin is only literally accurate but really somewhat misleading in saying that this effect "offers an additional factor of safety." In fact, he appears to recognize this, in the next sentence, by admitting that the self-extinguishing effect "allows working voltage stress to more safely approach the critical voltage stress at which cumulative ionization deterioration occurs."

It would help an understanding of this important type of cable if Mr. Shanklin would clarify this matter of voltages stresses, in detail, in the light of his experience and research.

Mr. Shanklin's statement that he has "tried about all of the recognized available types of high-viscosity compounds, including rosin mixtures, and so forth" is probably a more comprehensive statement than he intended it to be, as each laboratory has its own line of development which overlaps with others to some extent but, in the main, proceeds along independent lines, with its own materials, processes, equipment, and methods of test.

I cannot help believing with Mr. Faucett and associated authors, that some of the recognized and maligned blended oils will give better results than the so-called "standard all-mineral compound used for some time past." This last-named compound is capable of becoming electrically unstable as the result of contamination in the impregnation process and should be easily surpassed, for this service, by compounds more permanently immune to ionization.

Also, the particular oil referred to by Mr. Shanklin is entirely incompatible with rosin or rosin derivatives, and cables made with such mixtures are quite unstable, a fact which may explain his aversion to the blends.

Table I

Faucett, Komives, Collins, and Atkinson	Shanklin
1. The saturant used in the Detroit cables has a very high viscosity	Optimum viscosity is 100 Saybolt at 100 degrees centigrade. (This is about the lowest viscosity ever used in solid-type cables)
2. Since it was desirable not to expose the insulation to the atmosphere any longer than necessary, and so forth	Cable exposed to air for eight hours had good endurance at 120 volts per mil but had increased dielectric loss
3. Very thin tapes result in increase of dielectric strength	Ionization voltage with three-mil paper was no higher than usually obtained with standard six-mil tape. Life test with three-mil paper "little if any longer"
4. Cable with thin paper withstands, without damage, normal bending	Thin paper tends to wrinkle and will not withstand cable-bending as well
5. Strand shielding of *SMD* cable believed responsible for its having higher dielectric strength than unshielded oil-filled cable	Ionization voltage same with and without strand shielding, and time to stabilize at 85 volts per mil same

Data on impulse dielectric strength given by R. Davis[3] indicate that highly drained cable at atmospheric pressure has an impulse strength of only about one-third that of normally impregnated cables. This points definitely to the desirability of using viscous compound to obtain minimum drainage and maximum impulse strength.

Mr. Faucett and his associate authors do not share Mr. Shanklin's belief in a low-viscosity impregnant. On the contrary, they are insistent on the use of one of "very high viscosity."

This brings me to the very interesting contrast between the views of these authors on several important points as shown in parallel columns in Table I.

Turning now to paper 42-135 by Faucett, Komives, Collins, and Atkinson, we find a cable system which is virtually the Oilostatic system without the oil and oil-pressure accessories. It should therefore be interesting from a first-cost standpoint.

Whether it will prove economical eventually may not be known for years, depending on how much cable is ruined by water entering the pipe, how much will be perforated by transient voltages or even working voltages in the event of loss of gas pressure. The Detroit Edison Company deserves the thanks of the industry for taking these risks in the interest of progress.

Table II

	Power Factors—Degrees Centigrade	
	85	25
At beginning	0.76	0.42
After 100 load cycles	2.47	0.41

The paper gives a great deal of interesting information among which the data on soil resistivity may be especially noted.

The formula, $g = 128/W(T_1 - T_2)$, while empirical, is derived from the following rational formula for a sphere:

$$G = \frac{4\pi r}{W}(T_1 - T_2)$$

where r = radius of sphere, and the other quantities are as given in the paper. This formula is based on the assumption that heat is dissipated equally, in all directions, from the sphere.

On the other hand, the Kennelly formula for heat resistance from a cable or pipe to ambient is based on the assumption that the heat is all dissipated upward. It is, therefore, obvious that the constants obtained by the authors' tests cannot be used directly in the Kennelly formula for determining the thermal resistance from pipe to ambient.

The formula for the test electrode gives a lower soil resistivity than the true one, because more watts are expended in driving heat through the nonuniform actual path than through a uniformly radial path.

On the other hand the Kennelly formula gives a falsely high resistance, because it neglects the very considerable amount of heat which flows in other directions than upward.

Therefore, if we use an unduly low resistivity in the Kennelly formula, we tend to counteract the falsely high resistance which that formula gives. Judging by *BEAIRA* publication *F/T*128, these errors cancel, for all practical purposes, as we find, that the usual correction factor of $^2/_3$, used in connection with the Kennelly formula, in British practice, is "*unnecessary when the thermal resistivity of the ground is measured in situ by one of the methods described in the appendix*." This is very important to note in view of existing controversies on soil resistivity.

For some years past I have been contending in favor of the use of a soil resistivity of 180 for standard carrying-capacity tables, using it in the Kennelly formula with the

Table III

	Power Factors—Degrees Centigrade	
	60	25
At beginning	1.23	0.45
After 20 load cycles	1.44	0.47
Increase, per cent	17	4.5

usual correction factor of $^2/_3$ so as to give a net value of 120.

Others have contended for a net value of 80 or less for this purpose.

The data presented in Figure 4 of the paper indicate the following safe net values, all of which are to be used *without* the $^2/_3$ correction factor.

Porous loam	100
Clay	105
Sand	120

This suggests a basis of compromise on a net value of 105 except for sandy soil, for which a net value of 120 should be used.

Values as low as 80 are attained only with porous loam during the three coldest months and are, therefore, unsafe.

It should be noted that these values would have to be increased something like 50 per cent for comparison with resistivities, derived from measurement of soil between definite boundary plates, such as given by Shanklin.[3]

The authors use, as criterion of stability of their cable, the power factor at room temperature or less, giving as reason that "it was not feasible to obtain power-factor measurements at elevated temperatures, since it required a relatively long time to switch the experimental cable out of the circuit and connect up the power-factor measuring equipment." This reason seems somewhat inadequate, as it is well known that room-temperature power-factor measurements are not a sensitive indication of deterioration. Data on cables life-tested in accordance with the procedure described by Boehne and Linde (page 207 of reference 4) are typical of results on laboratory made samples of definitely unstable character, as shown in Table II.

Similarly a cable of poor quality, life-tested in accordance with the procedure described on page 209 of reference 4 gave the results shown in Table III.

Here the room temperature reading showed some change but only about one-fourth that at 60 degrees centigrade.

The instability of these cables would have passed unnoticed if room temperature power factors had been relied upon.

REFERENCES

1. Discussion by **William A. Del Mar** of IMPULSE STRENGTH AS A MEASURE OF CABLE QUALITY. AIEE TRANSACTIONS, volume 60, 1941, page 1340.

2. **R. Davis.** *Journal* of the Institution of Electrical Engineers, April 1942, part I, number 16, page 201.

3. EFFECTS OF MOISTURE ON THE THERMAL CONDUCTIVITY OF THE SOILS, **G. B. Shanklin.** AIEE TRANSACTIONS, volume 41, 1922, pages 94–100.

J. A. Peterson (The Commonwealth and Southern Corporation, Jackson, Mich.): Three types or methods of installation of high-pressure gas-filled cable are mentioned in paper 42-135:

1. Method used by the Detroit Edison Company
2. English method with reinforced lead sheaths.
3. Oil-filled (Oilostatic) method.

Is there any information available as to the relative costs of these three methods?

It will be interesting to know the expected life of the iron pipe treated with one-half-inch Somastic as used in the Detroit Edison method.

The data on soil thermal resistivities constitute very valuable information, especially to the Detroit Edison Company, but it would be more valuable to the industry as a whole if the actual moisture content of the various types of soil were known for the different values of thermal resistivities. Was this point checked at the time the tests were made?

It would be interesting to know how conductor expansion is taken care of on this installation—by the lay of the cable, by the snaking effect of the cable in the pipe, by expansion bends, or by a combination of these methods. The report states that in the experimental installation the expansion of the iron pipe was taken care of by expansion bends. Was this method used for the entire installation?

The viscosity of cable-impregnating oil used in low-voltage solid impregnated-paper lead-covered cable ranges ordinarily from 100 to 150 seconds Saybolt Universal at 100 degrees centigrade. The viscosity of the oil used in the gas-filled cable is given as 3,000 seconds Saybolt Universal at 100 degrees centigrade. What is the viscosity of this latter oil at 50 and 150 degrees centigrade? What is the maximum temperature at which this cable is expected to operate? Will this impregnating oil remain in the insulation, or will it gradually drain out into the pipe, and, if it drains into the pipe, will it be necessary to drain the oil from the pipe? It is possible that the oil will remain in the insulation and that none of these points present any problem.

Johnstone Wright (Central Electricity Board, London, England): The first departure from conventional solid-type underground-cable practice in Great Britain was an extensive application of oil-filled cables in sections of the British Grid. Such oil-filled cables were used at 66 kv and 132 kv. They have been quite successful, but their use is attended by certain disadvantages, particularly where the contour of the cable route includes steep sections. We were, therefore, interested in any developments which would minimize these difficulties and at the same time be economically attractive.

A gas-pressure cable of the Hochstadter type was, therefore, laid in a London area and put into service on October 1, 1932. This cable was laid in a steel pipe which has given rise to a certain amount of electrolytic corrosion trouble. The source of this trouble was discovered and the necessary remedial measures instituted, and subsequent experience has been all that might be desired. We are, nevertheless, somewhat apprehensive regarding the possibilities of corrosion in steel pipes, and a subsequent gas-pressure cable in the same area is of the double-lead-sheath type. At no time has there been any electric fault on either of these gas-pressure cables.

When the internal gas-pressure cable was mooted, we took a close interest in its development, and when all the necessary works proving tests had been satisfactorily carried out, we put a one-mile trial length of 132-kv cable into service as a field experiment. This cable went into use on December 4, 1937. It has carried the loads, short-circuit currents, and surge stresses encountered during the intensive bombardment of London without trouble. The cable was on one occasion damaged during street excavation, and the necessary repairs were effected expeditiously, the circuit being put back into service much in the same way as is done in everyday conventional mains practice.

The success of this experimental cable led us to utilize the same type of two further short 132-kv routes where it was impracticable to have overhead lines. We are now engaged in the final stages of making a seven-mile circuit of 132-kv gas-filled cable.

This outline cannot do justice to the excellent work which has been done by the manufacturers in producing economical and reliable 132-kv underground cable, but the detailed story must be left to be told when conditions become more normal.

L. Meyerhoff (General Cable Corporation, Bayonne, N. J.): The information contained in the paper on 120-kv high-pressure gas-filled cable regarding temperature stability is amplified and brought to date by the following.

Two lengths of cable representative of that furnished for the commercial run were cut up into a number of 12-inch lengths. These were individually sealed in lead pipe, the free space in the pipe being filled with nitrogen at about five pounds gage pressure. The sealed samples were then placed in an oven and maintained at 100 degrees centigrade. Samples were withdrawn at intervals for test purposes.

In addition to the above samples, two samples from each length were equipped with electrodes and guard rings before being sealed into the lead pipe, and leads were brought out through insulating bushings so as to permit electrical measurements to be made at 100 degrees centigrade during the progress of the aging run.

In Figure 2 of this discussion is shown the variation of power factor at 100 degrees centigrade with time of aging on the four last mentioned samples. It will be noted that the power factor after 220 days of aging has changed very little, what change there is being apparently in the direction of improvement.

Figure 3 shows the temperature-power factor data for several samples of one of the cables, tested after various aging times up to 184 days. The points for all the samples (including the unaged cable) are represented very well by a single curve. The variations between these samples are no greater than would be expected of a similar number of individual samples of new cable.

Figure 4 shows the radial power factor at 80 degrees centigrade for the cable in its original condition and after 40, 113, and 184 days of aging. It will be noted that in the

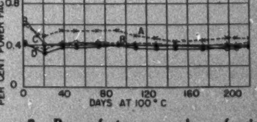


Figure 2. Power factor versus days of aging at 100 degrees centigrade

A, B, C, and *D* represent four similar samples

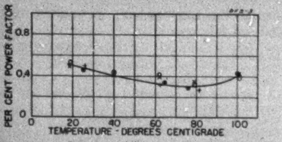


Figure 3. Power factor versus temperature
Showing effect of aging at 100 degrees centigrade for 184 days to be insignificant

× New cable	+ Aged 113 days
○ Aged 40 days	● Aged 184 days

main the radial power factor is quite flat in all cases and that such changes as appear are in the direction of reduction rather than rise of power factor. In the case of the last two samples withdrawn there appears a slight rise in power factor near the sheath. Whether this rise is due to the aging or is characteristic of the particular samples will not be known until further samples are withdrawn and tested.

In Figure 5 the tensile and tearing strength values have been plotted for the new cable and for a sample which had been aged for 184 days. It is apparent from these results that there is no material change in mechanical strength. The sharp change at 20 per cent distance from the conductor coincides with a change in the type and thickness of the paper.

Thus, from all the standpoints studied there has been no significant change in the electrical or physical properties of the cable during more than six months' aging at 100 degrees centigrade.

I should like also to discuss the benefit of using thin paper in gas-filled cable. Our tests have indicated that gas-filled cable with thin paper has considerably higher breakdown strength than similar cable with thick paper, particularly on short-time and surge tests. The difference in strength appears to be greater than can be justified on the basis of the increase of the specific breakdown strength of gas, by reason of the reduction of the gap distance. From the published data, the breakdown strength of a 2.5-mil gap of air at 200 pounds pressure is about 12 per cent higher than that of a five-mil gap at the same pressure, whereas gas-filled cable made with 2.5-mil paper is apparently considerably more than 12 per cent stronger electrically than cable made entirely with five-mil paper. Thus, the above explanation seems numerically inadequate.

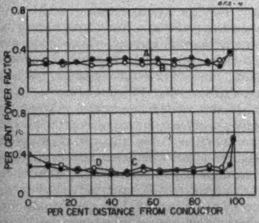


Figure 4. Radial power factor as affected by aging at 100 degrees centigrade

A. New cable	C. Cable aged 113 days
B. Cable aged 40 days	D. Cable aged 184 days

What appears to be a more adequate explanation is the following. The benefit of the thin layer does not lie so much in the voltage at which the gap breaks down as in the reduction of the amount of energy dissipated in any gap after it breaks down, that is, above ionization voltage.

Compare two cables of the gas-filled type, one, cable *A*, made up of five-mil paper and the other, cable *B*, of $2^1/_2$-mil paper. Assume the power-factor versus voltage characteristics of the two cables as a whole to be identical. Thus, at any voltage above the ionization point, the total energy dissipated in all of the gas spaces will be the same in cable *A* as in cable *B*. However, since cable *B* has twice as many gas spaces as cable *A*, the energy dissipated in any one gas space of cable *B* will be only one half as much as in a similarly placed gas space of cable *A*. This means that in any $2^1/_2$-mil space there is only one half as much energy ready to bombard and start to burn the paper and oil or to find a path around the paper as in a corresponding five-mil space.

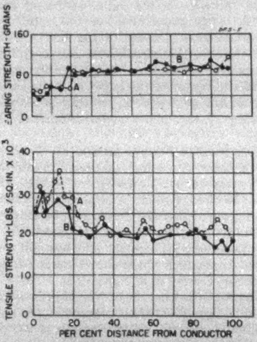


Figure 5. Mechanical strength of insulation as affected by aging of cable at 100 degrees centigrade

A. Cable aged 184 days

B. New cable

Dawes[1] has found that the energy loss caused by the ionization of the gas spaces in a cable may be represented approximately by the expression $K(E - E_0)$, where K is a constant, E the voltage applied, and E_0 a voltage substantially equal to the ionization voltage. Thus, in order for the energy in one gas space to be the same in the two cables, $E - E_0$ must be twice as great as in the case of cable *B* as in the case of cable *A*. For instance, if E_0 is 140 kv for the two cables, 200 kv applied on cable *A* will produce the same amount of energy in a given gap as 260 kv will produce in a corresponding gap of cable *B*.

In the above discussion it was assumed that the power-factor versus voltage characteristics of the two cables are the same. As previously stated, however, at 200 pounds per square inch a $2^1/_2$-mil gap begins to ionize at a 12 per cent higher voltage than does a five-mil gap. Thus, in changing from five-mil to $2^1/_2$-mil paper there is a gain in breakdown strength of this 12 per cent plus the additional gain indicated in the above discussion. The amount of this additional gain will depend on the ionization characteristics of the cable and on the voltage applied.

In tests of long duration the value of $E - E_0$ is small, so that the gain in strength caused by the use of thin paper in such tests may be substantially only that, caused by the increase in ionization voltage. In short-time tests, however, $E - E_0$ is large, and the gain in strength will be very considerable. On the basis of this analysis, the gain should be even more marked in surge tests. It should be noted further that at lower gas pressures, say 30 pounds per square inch, a given amount of gain due to the thin paper will occur at a lower voltage than at the high pressure because of the large reduction in the ionization voltage. This fact makes it feasible to test cable with $2^1/_2$-mil paper for acceptance purposes at much lower pressures than would be permissible for cable with five-mil paper.

REFERENCE

1. IONIZATION STUDIES IN PAPER-INSULATED CABLES—II, C. L. Dawes, H. H. Reichard, P. H. Humphries. AIEE TRANSACTIONS, volume 48, 1929, pages 383–98.

C. Beaver (W. T. Glover and Company, Ltd., Manchester, England): The authors of paper 42-135 and all concerned are to be congratulated on the successful introduction into the United States of the high pressure gas-filled cable on a commercial scale. It is to be hoped that it will have an operating experience similar to that of gas-filled cable in England.

It is, of course, essentially the same cable but differs in the method of installation and in this important respect would appear to carry a greater burden of risk. That is to say, the difficulty of maintaining long lengths of pipe line free from leakage under high gas pressure would appear to be considerable, whereas experience shows that perfect freedom from leakage is obtainable with the reinforced sheath type. In actual practice in England no supplementary gas supply is ever necessary after the initial charging.

Under conditions of laying and installation obtaining in England, it is very doubtful whether the pipe-line method would ever be used; the self-contained perfectly gastight reinforced sheath type having everything in its favor from the user's point of view, that is, embracing economical, technical, installation, and maintenance aspects.

The substantial character of the protective coatings of the pipe is noted, but it has to be remembered that in the presence of vagabond currents in the vicinity of a minute imperfection or damage the current density at such a point might be relatively high and corrosion correspondingly rapid. In other words, the very excellence of the protection from ordinary chemical attack entails possible susceptibility to concentrated electrolytic corrosion.

It is gratifying to note that the authors consider this type of cable to be at least equal, electrically, to the oil-filled cable, and that, physically, the type is superior to the oil-filled cable in that

(*a*). No draining or migration of the impregnating oil is encountered, so that stop joints or other devices are not required.

(*b*). No allowance has to be made for impregnating oil expansion so that oil reservoirs are not necessary.

In the reinforced lead sheathed cables such as have been installed in England the maximum operating conductor temperature is 80 degrees centigrade, and at this temperature the gas-pressure rise is such that the gas density at the conductor surface, where the electric stress is highest, is approximately equal to that at 15 degrees centigrade, and in consequence the ionization voltage is substantially the same at 80 degrees centigrade as at 15 degrees centigrade. (It should be noted that these cables are sealed off and are not in communication with gas reservoirs at the terminals.) In the pipe form of the cable the gas-pressure value at high-conductor temperature will not increase proportionately to the absolute temperature, and hence the ionization voltage will be lowered at high-conductor temperature. This is fundamentally due to the fact that the average temperature of the gas-filled system in the pipe type of cable does not increase much with increase of conductor temperature.

The authors have given the reasons for the radial grading of the paper thicknesses in a very clear and concise manner. In England the thinnest paper tapes used at present are $1^{3}/_{4}$ mils thick, and the ionization voltage at 200 pounds per square inch by gauge of a 600,000-circular-mil round conductor cable insulated with 600 mils of paper, of graded thickness $1^{3}/_{4}$ mils to $5^{1}/_{2}$ mils, would be 165 kv minimum between conductor and sheath, the permittivity of the dielectric being 2.9 to 3.0. Cables have operated at maximum stress values of 335 volts per mil at 200 pounds per square inch by gauge in England for nearly five years as compared with the value of 288 volts per mil at 255 pounds per square inch by gauge for the installation described in the paper.

It has long been known that increasing the density of paper results in high breakdown voltages in solid-type dielectrics. In the gas-filled cable a particular advantage accrues under impulse voltage conditions, because the gas-filled spaces shunt their stresses to the solid (impregnated-paper) component of the dielectric, and the breakdown occurs due to disruption of this part of the dielectric.

As a matter of fundamental design the provision of conditions which suppress ionization up to twice working voltage also provides the dielectric with an asymptotic a-c time–breakdown value sufficiently above the working voltage to take care of such margins as manufacturing variations and installation exigencies, together with sufficient impulse strength to resist the maximum impulse voltages encountered in service.

The authors' work on the latter aspect is very valuable as showing the influence of various factors on the impulse strength of the gas-filled dielectric.

No data are given in the paper as to permittivity and thermal resistivity values. In the cables installed in England the permittivity value is 2.9 to 3.0, while the thermal resistivity value is 700 thermal ohms per cubic centimeter. These cables are insulated with paper dried and preimpregnated in sheet form on a special vacuum paper serving machine (British patent 373,697). On this machine the impregnated sheet of paper is scraped, while hot, by means of heated knives, so that all excess or free impregnating compound is removed. The roll of impregnated paper 15 inches in diameter and 36 inches wide is split into spools and then lapped on to the cable in air with no control of the humidity. The power factor–temperature characteristic is practically identical with that given in Figure 12 of the paper. The cables insulated with this paper can be successfully subjected to mine-shaft cable drainage tests at 80 degrees centigrade.

The authors' remarks in the third paragraph of the section dealing with dielectric strength, regarding the origin of the failures under prolonged a-c tests are interesting.

We have also noticed in similar tests using high-impedance circuits and short time relays to limit the burning at the fault that the puncture generally occurs in the gas-filled space within a distance of ten mils from the edge of the paper strips for gaps width of the order of 60 to 100 mils. We attribute this location of the fault to the fact that the thickness of the gas space here is equal to the paper thickness, whereas toward the center of the gap width the thickness of the gas space is reduced because of bowing of the paper strips bounding the gap.

Referring to Figures 10 and 11, the data given show the effect of moisture on the high-temperature power-factor values and also the effect of control of humidity during the lapping process. According to English manufacturing experience, however, it is not necessary to control the humidity in order to produce power-factor values equal to those of curve *A* in Figure 10.

Further, the absence of free impregnating oil in the gas-filled cable eliminates the possibility of metallic contamination of the dielectric by the copper-conductor screening tape, or lead sheath during temperature cycles. This phenomenon has of course been noted frequently in cable in which the dielectric is filled with oil.

I note that there is little reference in the paper to physical features except with regard to paper density and viscosity of the impregnating medium, and perhaps, without being too elementary, I may be allowed to point out that the high-pressure gas-filled cable—at least in its reinforced sheath form—has an important physical basis of design. It strikes at the root of the ionization troubles which limit the use of solid-type cables, by substituting completely reversible conditions of expansion and retraction inside the confines of the cable sheath for the partially reversible conditions which are responsible for the said limitations.

In providing for expansion and contraction at constant volume it also obviates "cold working" of the lead sheath—a result not obtained in any other type of cable.

The corollary is that the original conditions established in the cable, as regards freedom from ionization, are never subsequently infringed.

In conclusion, the authors' figures for mileages of high-pressure gas-filled cable installed in England may be brought up to date as follows:

Installed:
132 kv—6.5 conductor miles
33 kv—52 conductor miles
(A considerable proportion of this has been in service 5 years.)
Proceeding, about to be put into service:
132 kv—18 conductor miles
33 kv—25 conductor miles

G. B. Shanklin (General Electric Company, Schenectady, N. Y.): My discussion of the two papers by Faucett, Komives, Collins, and Atkinson will deal chiefly with the high-pressure gas-filled type of cable, but I will first comment briefly on the compression-type cable.

We made a close study of the latter type when it was first introduced about ten years ago. The main thing that discouraged us from further work along this line was the fact that safe operation of this design depended upon complete gastightness of the lead sheath, joint sleeves, and wipes. If high-pressure gas in the steel pipe, normally segregated from the cable by the impervious sheath covering, once enters the cable cross section through a haphazard leak, it will gradually saturate the impregnated paper and travel a long distance back in the cable length. Eventually at some distant point gas will replace impregnating compound forming a "dry spot." There is then no means of maintaining sufficient pressure at this point when temperature changes take place, and ionization trouble will occur. Experience with all cable systems indicates that it is not possible to avoid occasional leaks in the sheath envelope.

Concluding that eventual gas saturation and loss of pressure control in compression-type cable were unavoidable in any case, we reasoned that it would be better to accept this fact at the beginning and use gas-filled insulation directly exposed to the high-pressure gas in the steel pipe. In this way full control of pressure could be maintained both radially and longitudinally; also, it represented a simpler and more economical design of pressure pipe cable.

Following out this idea we started initial development work in 1932, the principles of which are described in United States patent 1,991,230, filed in July 1933. Some of the test results are outlined in a paper, "Low-Gas-Pressure Cable,"[1] which I presented at the AIEE winter convention in 1939. Further details relating to this cable system are given in a second paper which I presented at this afternoon's meeting, contemporaneously with Faucett, Komives, Collins, and Atkinson's paper, "120-Kv High-Pressure Gas-Filled Cable."

The cable systems described in these two papers are closely the same in principle, the only difference being the use of mass-impregnated-paper insulation in one case and preimpregnated paper in the other. In pointing this out, I have no desire to detract in the slightest from the credit due Faucett, Komives, Collins, and Atkinson for the initiative they have shown in going ahead with the first full-sized installation of this kind and the perfection of practical detail it involved. They are heartily congratulated for carrying on to a finish the fine job that was done.

On purely technical grounds I am wondering why Mr. Faucett selected preimpregnated paper in preference to regular mass-impregnated-paper insulation for his system, although I readily admit that at 200 pounds nitrogen pressure either type is safe for 120-kv service. The reason for this is that the electrical strength of nitrogen gas at 200 pounds pressure is almost as great as that of the solid insulation.

H. M. Hobart of our company demonstrated this in his development of a high-pressure gas cable system in which bare rigid copper conductors were held in place in a steel pipe by insulator spacers at intervals. Installation difficulties and low impulse strength were the factors that prevented commercial use. Sixty-cycle strength was sufficient for 138-kv service at pressures in the order of 200 pounds and above.

Dry unimpregnated-paper insulation also has sufficient 60-cycle strength at 200 pounds gas pressure for high-voltage service. Low impulse strength and the difficulties of moisture absorption are its chief handicaps. It is absolutely essential to operate dry-paper cable well below the ionization starting voltage, which, at reduced pressure, is lower than that of impregnated paper because of the relatively large volume of void space involved. Ionization action in this type of cable is cumulative, and failure will occur quickly after incipient ionization once starts.

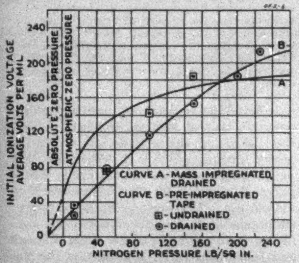


Figure 6. Voltage stress on solid insulation at which initial ionization starts in drained gas-filled cable

Preimpregnated-paper tape applied dry without flushing compound, as used by Mr. Faucett, overcomes the difficulties of ready moisture absorption and low impulse strength, but in all other characteristics it is about midway between dry paper and mass-impregnated paper. At butt spaces, wrinkles, tears, and other such points of nonhomogeneity, the void spaces are just as large as with dry unimpregnated paper. Ionization-starting voltage as a function of gas pressure is accordingly about the same, but incipient ionization is less dangerous because of the barrier effect offered by the impregnated tapes. It is important, however, to operate well below the ionization-starting voltage and eliminate any possibility of incipient ionization. There is no surplus compound surrounding the void spaces, which are not only larger for this reason but lack the ability to extinguish incipient ionization by wax formation and self-healing action.

This characteristic would be more clearly understood if Mr. Faucett had presented charts showing power-factor change and general behavior on long-time full-temperature load-cycle endurance tests at voltage steps always a little above ionization starting voltage. We are running such tests now at 200 pounds pressure on both mass-impregnated and preimpregnated insulation.

In the meantime Figure 13 of Mr. Faucett's paper plainly shows the trend of this characteristic for preimpregnated cable. Even at 225 pounds pressure the cable always failed in from 4 to 16 days at a voltage only a little above initial ionization-starting voltage. This proves a lack of self-healing action such as exists in mass-impregnated gas-filled cable. The latter type would not have failed at all under these conditions. Incipient ionization would have been extinguished, and the cable would have remained stable indefinitely, necessitating one or more higher voltage steps and a longer time to cause failure. Reference to the test charts in my paper will confirm this.

A comparison of initial ionization-starting voltage stress as a function of gas pressure tells the whole story. Such a comparison is given in Figure 6 of this discussion where the ionization data for preimpregnated insulation, from Mr. Faucett's Figures 8 and 9, are superimposed on a corresponding curve for mass-impregnated insulation, from Figure 5 of my paper. It will be noted that at 200 pounds pressure there is practically no difference between the two types of insulation. For 120-kv service either could operate safely at the usual average working voltage stress of 140 volts per mil, although gas pressure could safely drop much lower for the mass-impregnated type without danger of ionization trouble.

When the same comparison is made with low- and medium-pressure systems there is a wide difference and, in fact, the preimpregnated type of insulation is not economically feasible in this range because of the heavy wall of insulation that would be required. Low-pressure gas-filled cable of the mass-impregnated type operates at an average voltage stress of 65 to 70 volts per mil and average gas pressure of 12 pounds. The ionization-starting voltage stress of the preimpregnated type is less than half the required working voltage stress. Medium-pressure gas-filled cable, mass-impregnated, is designed to operate at 95 volts per mil, 30 pounds gas pressure. Here again the ionization voltage of the preimpregnated type is less than half the required working voltage stress.

In Beaver's high-pressure gas-filled cable with reinforced sheath, preimpregnated insulation was necessary, for he had no means of transmitting and controlling gas pressure along the cable length, other than the small butt spaces between tapes. In a high-pressure pipe cable system, however, far better pressure control exists than Beaver could ever hope for, and, in my opinion, the need of preimpregnated insulation no longer exists.

There is another important point to be considered. Aging studies with impregnated paper show that the rate of absorption when exposed to oxygen and moisture, as well as the rate of deterioration from this cause, are inversely proportional to the viscosity of the impregnating compound. For a given quantity of absorbed impurities, however, the ultimate results are the same. It merely takes a longer time to reach these results when heavy compound is involved.

This being so, it is just as important to exclude all traces of oxygen and moisture impurities in a system using preimpregnated insulation as in one using mass-impregnated insulation. Considering the much greater degree of surface exposure of preimpregnated tape from the start of production up to final sealing of the completed system in the field, I do not see how it is possible to exclude impurities to the same degree. This problem is common to all types of impregnated-paper cable and not to gas-filled cable alone.

If anyone doubts the importance of excluding oxygen and moisture he is referred to F. M. Clark's paper, "Factors Affecting the Mechanical Deterioration of Cellulose Insulation,"[2] presented at the convention day before yesterday. The effects of even traces of oxygen are already well known, but Mr. Clark's conclusion that insulation life is halved for each doubling of moisture content is not generally known.

REFERENCES

1. LOW-GAS-PRESSURE CABLE, G. B. Shanklin. AIEE TRANSACTIONS, volume 58, 1939, July section, pages 307–15.

2. FACTORS AFFECTING THE MECHANICAL DETERIORATION OF CELLULOSE INSULATION, F. M. Clark. AIEE TRANSACTIONS, volume 61, 1942, October section, pages 742–9.

E. Johansson (General Cable Corporation, Bayonne, N. J.): In the paper on 120-kv compression-type cable, breakdown values of 436 and 445 volts per mil of cable insulation are given for two joints tested. It may be noted that the joints used in these tests were not identical with the joints in the field installation but represented a stage in the development of the final joint. A subsequent test on a joint of the final construction produced a breakdown value of 602 volts per mil of cable insulation with the same test program and after a total time undervoltage of 11 hours.

The difference in performance between the two types of joint is explained by the construction, a small quantity of varnished cloth having been used in the first two joints but paper alone in the later joint. The copper conductors are elliptical, so that the cross sections of the two conductors to be joined will not line up normally. This discrepancy is accommodated by using a specially shaped twisted elliptical connector. In order to produce smooth insulation over the connector it was thought essential at first to use a few layers of varnished cloth next to the connector, but later development permitted satisfactory use of paper tapes even at the connector.

In applying paper tape to these joints, special care was taken to minimize access of moisture to the paper. The splicers wore rubber gloves, and the insulation was flushed with hot oil at intervals. As a result there was only a slight increase in the power factor of the tape when applied under severe conditions, that is, during the days in the summer when the relative humidity was high (65–70 per cent).

The following comments supplement the

paper on 120-kv high-pressure gas-filled cable:

One stop-joint for this cable was given a 60-cycle dielectric-strength test. The joint was first subjected to a constant voltage of 140 kv for 101 hours at 225 pounds per square inch gas pressure; then the voltage was increased five kilovolts every 24 hours to and including 180 kv, making the total time under voltage 293 hours. The gas pressure was then lowered to 100 pounds per square inch, and 140 kv was again applied for 34 hours without failure. The preceding test is well above the 170 kv for six hours required under the Association of Edison Illuminating Companies specification for samples of 120-kv oil-filled cable. The dissection of the joint showed no visible effects of electrical stress.

As stated in the paper, tests were made in the laboratory to determine the amount of drainage in the cable at high temperatures. A cable was heated to 100 degrees centigrade in a vertical position for 43 days, and the space occupied by the gas in the insulation was measured before and after heating. The proportion of the insulation occupied by gas increased from five per cent before heating to nine per cent after the 43 days of heating. This determination was made on a sample taken from the top of the inverted *U*, where the maximum drainage took place.

The paper describes incipient or arrested failures produced under test conditions. The following may be added concerning other characteristic appearances of these incipient failures. In a number of cases, burning or tree design was found in layers of paper remote either from the conductor or the sheath, and no apparent connection was found between these and the conductor or the sheath. However, on extraction of the compound from the tapes lying between the conductor and the failure, it was possible, by examination through a microscope, to find punctures in these tapes. These took the form of a large number of small holes and could usually be found in all tapes down to the conductor. It is felt that, while this connection was not actually found in all cases, a sufficiently careful and complete examination would have shown a puncture extending to the conductor in each case.

J. B. Whitehead (The Johns Hopkins University, Baltimore, Md.): Referring to the two papers on gas-filled cables, the results presented show a fine record of progress in experimental and manufacturing development. The principle involved in the fine performances reported is the elevation of gas pressure to a point where the upward break of the usual ionization curve is pushed up to a voltage sufficiently above the operating voltage. However, it must be evident that ionization may be present in small quantities owing to surface irregularities in the gas spaces and other local causes of high stress.

My question is directed particularly to the long-time influence of this small residual ionization. Evidence of its importance are found in the data in the Faucett paper and in noticeable changes in the position of ionization curves as between Figures 8 and 9 of the paper. Mr. Shanklin directs attention to the increase and subsequent decrease of power factor caused by wax formation in Figure 7 of his paper. But even here the presence of residual ionization is indicated by the slightly greater ionization factor in the range 20 to 60 volts per mil at the end of 252 days than at the beginning. Moreover, substantial local ionization is possible without reflection in the value of over-all power factor. Indeed the latter is usually not affected until the ionization assumes such large proportions that breakdown is immediately imminent.

The authors agree that further studies and performance tests are necessary for the gas-filled cable. I venture the hope that future studies will include that of the influence of local ionization of small volume on the immediately adjacent insulation wall.

Referring further to the Faucett paper on gas-filled cable, will one of the authors please give the numerical data back of the statement on page 663, beginning of column 3? What is the over-all specific inductive capacity of the gas-filled cable as compared with that of the same cable before draining? Somewhere in these papers it is stated that comparatively little oil is lost in the draining process. It would be interesting to have the figures leading to the conclusion that the stress in the gas spaces is less than that in other types of insulation.

In the same paper it is interesting to find such very clear data indicating in Figure 13 that the approximate life to failure varies inversely as the 7.5 power of the voltage. Heretofore data on this question have been very meager. As to the dielectric strength–time behavior below one hour, the impulse tests reported indicate that toward very short time values, the curve of Figure 13 will again turn up. This would result in a curve closely similar to those for other types of insulation as reported by Montsinger and others.

K. S. Wyatt (Phelps Dodge Copper Products Company, Yonkers, N. Y.): This discussion of the three papers describing extra-high-voltage cable designs which employ compressed gas parallels the session on the same subject at the Paris conference (CIGRE) in 1939. My contribution to that discussion is on record, and my remarks then hold good today. One of these designs (compression cable) employs compressed gas external to the lead sheath merely as a mechanical means of compressing the cable on the cooling cycle to insure freedom from vacuous and gas spaces; at the other end of the scale are the two designs which employ compressed gas in the insulation itself. These three papers evidently indicate that we are entering a transition stage in extra-high-voltage cable design: the new designs, with high-pressure gas-filled insulation, are challenging the older conservative designs, based on keeping filled with oil all spaces within the cable that are not filled with solid material, namely, the oil-filled, the compression, and the Oilostatic cables. The end result will no doubt be that no one design will be employed exclusively for all conditions, that is, each of several designs will find application in a restricted field.

At the time of the Paris conference in 1939, Beaver had a semi-commerical gas-filled cable at 132 kv installed as a loop of about one mile length on the Central Electricity Board system in England with more than a year's operating experience. He had also in operation for several years a number of gas-filled cables at 33 and 66 kv. Kirsch of Berlin (AEG), and Schneeberger of Brugg (Switzerland), were talking of installing operating lines at 33 kv with thick insulation and low gas pressure and gradually feeling their way to over 100 kv by increasing the stress and gas pressure. Callenders were installing at this time about a mile of 66-kv gas-filled cable of the loose-sheath type in London. While I believe an insulation thickness of 240 mils was employed, the fact that they wished to use only 100 mils for 33 kv indicates their faith in this design. Those who adhere to the principle of filling the insulation with oil rather than with gas should not lightly pass by these new designs, for they merit careful consideration.

The authors and the Detroit Edison Company in making these investigations of the compression cable and gas-filled cables at 132 kv have performed a great service for the industry. It is only to be regretted this was not done five or ten years earlier. The paper on the compression cable makes two important contributions:

1. The studies confirm for American loading conditions and conductor sizes the considerable body of work done abroad continuously since 1926.

2. A highly important design feature has been introduced, which, I understand is due to L. I. Komives, namely, the use of three S.L. cores cabled together with short lay without binders to cushion longitudinal expansion thrusts on the joints, throwing into the discard the cumbersome steel wire armor of the European design. (Europeans like to complicate, Americans to simplify and make foolproof). This gives us a simplified design which may well be called American-type compression cable.

I should like to point out that the terminal design is that of Siemens Brothers of London; so also is the joint design, except the ferrule scheme, which is, I believe, the subject in England of an Enfield Cable Works patent but which for several reasons they did not use. The work on increased fatigue endurance of lead under nonoxidizing conditions is an extension of the work of Haigh and Jones,[1] and of H. J. Gough, of the National Physical Laboratories of London. In the paper by the same authors on gas-filled cable the use of 80 and 100 degrees centigrade power-factor measurements to detect moisture pickup of impregnated-paper tapes is due to R. C. Mildner (with T. R. Scott) who, because of his considerable work on this matter, I think should receive credit.

Figure 7 shows power factors at room temperature or below up to 0.5 per cent which appear rather high. My associates and I have manufactured and installed since 1939 four compression cables at 66 kv, the 20 and 60 degree centigrade power factors of which were as low as 0.2 per cent and never higher than 0.3 per cent.

The British manufacturers have agreed to reduce insulation thicknesses for the compression cable with a limit on maximum stress of 280 volts per mil, instead of 216 volts per mil. Even if the *SMD* cable is in future reduced 20 per cent as the authors suggest, to 560 mils, this construction will still be more bulky (see Table IV).

Gas-filled cable at the lower voltages is understandable; I find it difficult to see any advantage at 132 kv, however, either economic or technical. Consider first the materials for an *SMD*-type gas-filled versus a compression or oilostatic cable; the corrosion-protected steel pipe is common to both; the conductor size for the gas-filled cable will certainly not be smaller; the insulation thickness for the gas-filled cable in the present instance is 20 per cent greater; a lead sheath is put on all three types of cables but is stripped off at installation for the gas-filled and Oilostatic types, while in the case of the compression cable the cost of its approximately half-thickness lead sheath will be partially offset by the cost of stripping and by the greater amount of lead in the larger-diameter gas-filled cable;* the *SMD* cable is protected with a helical *D*-shaped copper armor wire, as is the Oilostatic, while the compression cable has two four-mil copper reenforcing tapes with fiber bedding and protecting tapes. The *SMD* cable therefore requires not less, but if anything, more materials and processing than the compression or Oilostatic designs.

Now consider the technical merits of *SMD*-type gas-filled cable versus compression or Oilostatic types: The factor of safety on long-time breakdown is about 2 for *SMD* as compared with 5 or 6 for compression and probably for Oilostatic cable. Thermal resistivity (insulation) for *SMD* is from 700 to 1,000 compared with 450 for cables completely filled with oil. In the event (rare, it is true), of a large hole in the pipe and loss of gas pressure, *SMD* cable must be taken out of service immediately, whereas with the compression type, the operator has 24 hours to make repairs. During installation in rainy weather or during operation the insulation is exposed to contamination in the *SMD* type, whereas with the compression cable the lead sheath protects the insulation. A large hole in the pipe might let in ground water which would not affect compression or Oilostatic types. Impulse strength of gas-filled cable, similar to *SMD* type, according to Beaver, is about ten per cent below, and according to R. Davis,[2] 66 per cent below standard types (for cable made with preimpregnated paper, but probably with gas films at atmospheric pressure). Many precautions against moisture pickup, and so forth, have to be taken during manufacture of the *SMD* cable because of the necessity for preimpregnating the paper tapes first and then applying them to the conductor in the factory atmosphere, but these are not necessary with compression or Oilostatic cable. The *D*-shaped copper armor wire is essential to the *SMD* type, not only for mechanical protection, but for carrying short-circuit currents, since there is no lead sheath to make contact with the *H*-type shielding. Not least important is the great difficulty of fault location for *SMD* cable in the absence of lead sheath.

Table IV. Comparative Dimensions of High-Voltage Cables

Single Conductor 138 Kv—750,000 Circular Mils

Type	Conductor Type	Conductor Outside Diameter (Inches)	Conductor Shielding	Insulation Thickness (Mils)	Lead Sheath Thickness (Mils)	Lead Sheath Re-enforcement (Mils)	Over-all Diameter (Inches)	Long-Time Breakdown Stress (Volts Per Mil—Maximum)
Oil-filled	1/2-inch hollow core	1.18	No	560	133	0	2.57	1,000**
Compression	Compack oval	0.90 (Maximum 1.02) (Minimum 0.78)	Yes	560	95	78	2.37 (Maximum 2.48) (Minimum 2.25)	
Oilostatic*	Compack round	0.908	Yes	560	94	0	2.46	
Detroit *SMD**	Concentric	0.998	Yes	672	94	0	2.81	409***

* Dimensions include a helical *D*-shaped armor wire 100 mils.

** Established for the compression cable in England and in Europe; after severe load cycles.

*** Faucett, Komives, Collins, and Atkinson; on new cable.

To my mind it is quite certain that some ionization occurs at spots in the *SMD* cable during operation. Although the authors use thin paper in the conductor region to keep down the radial dimensions of the butt spaces, wafers show that such a cable after bending, pulling, and installing, will have some gas spaces of ten mils and even 20 mils radial dimensions. These being filled with gas, not oil, will certainly ionize at operating voltage. The power-factor voltage curve will not necessarily show up local ionization pockets in a long length, as Doctor Whitehead has shown. The authors state such ionization would be dangerous to their cable, although Shanklin admits such ionization occurs but says it is innocuous. However, Shanklin's ionization spaces are sealed with compound, whereas in the *SMD* cable the ionization pocket is part of a continuous gas film from conductor to sheath. Before the authors can state that no ionization goes on in an operating *SMD* cable, they should submit evidence with the magenta dye test on insulation after a year's operation. Because of this ionization, I feel there is some doubt as to the permanence of the insulation of this type of cable, and careful laboratory evidence after a five-year trial period would be helpful.

While the *SMD* cable will no doubt work satisfactorily, it seems difficult to see the reasons for choosing it in preference to the compression cable. When you strip off the lead sheath, you throw away a very considerable factor of safety against breakdown, expose the cable to contamination, leave the operating man with no alternative in the case of a large hole in the pipe but to take the cable out of service immediately and make fault location difficult, without effecting any monetary saving. If the compression-cable system, American-type, were streamlined with styrene joints and styrene terminals to eliminate compensators and with styrene sheath insulators to eliminate sheath losses, it would seem to me to offer a great deal more to the operating engineer than the gas-filled types. To quote my concluding remarks at the Paris conference, "We do not think it should be taken for granted that the gas-filled cable cannot be made workable. At present, the gas-filled cable with breakdown at only 100 to 150 kv per centimeter maximum stress hardly comes up to the oil-filled and the compression cable with breakdown at 400 kv per centimeter or over at maximum stress. If the operating engineer is to demand a gas-filled cable with the same factor of safety as the oil-filled cable or the compression cable, it means that the gas-filled cable is going to be very bulky indeed in external dimensions, and it is highly doubtful whether it can come within great distance of the compression cable economically."

In this discussion we have been interested mainly in comparing the pipe-line-type *SMD* cable with other pipe-line-type cables. Where the flexibility of the duct and manhole system is desired, the oil-filled cable may be expected to hold its own.

REFERENCES

1. ATMOSPHERIC ACTION IN RELATION TO FATIGUE IN LEAD. *Journal* Institute of Metals (London, England), volume 43, 1930; *Transactions* Institution of Chemical Engineering (London, England), 1929.

2. *Journal* Institution of Electrical Engineering (London, England), volume 89, 1942, part II, page 52.

Herman Halperin (Commonwealth Edison Co., Chicago, Ill.): These three papers on cables employing gas at various pressures are of immediate practical value to some users. The people and companies behind these papers are to be congratulated. In my opinion, the selection of the type of cable to use should be based mainly on the economics involved and on matters relating to usage. By the latter, I mean not only reliability in service but also matters relating to construction, maintenance, time to make repairs, and personnel.

The first detailed discussion will be on the paper by Mr. Shanklin. His article states that to date experience indicates that the low-pressure gas-filled cable is satisfactory from a technical standpoint. On the other hand, solid-type cables operating at 12 kv, 22 kv, and 33 kv are giving satisfactory service. Aging tests, for example, on shielded three-conductor 12-kv cable in Chicago show that there is no appreciable change in ionization nor power factor after severe testing—in other words, it is perfectly stable. We have a low rate of troubles in service, and in any event we must expect, in laying out designs for service, that with any kind of cable failures may occur. If a man drives a steel point into a cable, then one will get a signal that there is trouble on a gas-filled cable, but anyhow the line will be out, and one will get the signal through the regular manner.

---

* The saving for the Detroit seven-mile line amounts to $16,400 less cost of stripping and freight, that is, probably about ten cents per foot per core.

For cable made with preimpregnated paper, but probably with gas films at atmospheric pressure see reference 2.

Another point with us is that we use parts of existing 12-kv lines to make up a portion of a new line in many cases, and the use of the solid type simplifies our activities in addition to saving money.

In connection with single-conductor cables, I wonder what trouble might occur because of oil slugs, even if the gas connections are paralleled, when there are severe differences in line profile. These differences will affect all three phases equally.

The power factor–temperature characteristic of this type of cable appears to be less favorable than that of solid-type or oil-filled cable. Figures 6 and 8 of the paper indicate that even after stabilization the power factors at 80 degrees centigrade are two to three per cent as compared with about 0.4 or 0.5 per cent for new solid-type or oil-filled cable.

It is surprising that neither grading of the tape thickness nor conductor shielding produced any effect in test results. Is it possible that by making these changes in cable construction other unknown variables were introduced which offset the expected effects?

From creep tests made by Professor Moore at the University of Illinois under the sponsorship of Utilities Research Commission, it appears that Asarco cable lead should be satisfactory for 225–250 pounds per square inch. Tests in a dummy manhole in Chicago indicate that Asarco cable lead containing about 0.018 per cent calcium will withstand bending in manholes at least as well as ordinary lead.

In my discussion of the two papers by Faucett, Komives, Collins, and Atkinson, I refer first to the commercial installation of the gas-filled cable.

One question of considerable interest concerns the thermal characteristics of these cables. For a given load how does the temperature rise from ambient to conductor compare for these cables and solid or oil-filled cables on the basis of one line or two lines? Also, what maximum temperatures are considered permissible for the gas-filled and compression cables for normal and emergency operation?

The thermal resistivities of soil shown in Figure 4 appear to be lower than those reported by others. More data would be of interest. Since the ionization point is a function of the pressure, it would be of interest to learn what variations in pressure are expected in normal operation.

Figures 14 and 15 indicate relatively good impulse strength for the gas-filled cable which, however, had conductor shielding as mentioned by the authors. In other reports based on the Detroit tests, Mr. Komives has shown that the thoroughness of impregnation and smoothness of conductor surface had marked effects on the impulse strength. In 1938 G. B. McCabe presented data before the Edison Electric Institute showing that drained impregnated paper had only 60 to 70 per cent of the impulse strength of well-impregnated paper. These five types of cable in the figures cover a considerable range in degree of impregnation of the insulation and should, we believe, show some differences in impulse strength. This point is important, because the thickness of insulation used in some of these high-voltage cables is determined largely by the impulse strength.

No data are given on the effect of drainage on impulse strength. That drainage does have an effect on the characteristics of the cable is shown by the fact that drainage reduced the ionization point from 140 to 130 kv and reduced the breakdown strength by ten per cent. What are the possibilities that, through the years of service, considerable draining of compound may occur to cause more severe reduction in quality?

Regarding high voltage–time 60-cycle tests on new cable, it seems from our data that the oil-filled cable is somewhat superior to the gas-filled cable, and the unit strength of the oil-filled insulation will not change in service. This disagrees with conclusion 5 but does not affect particularly the installation of commercial gas-filled cable which has 20 per cent more insulation than is required for oil-filled cable.

It is of interest that failures occurred in the region of wide interturn gaps. May this be due to the field distortion caused by combination of solid and gaseous dielectrics or due to the greater probability of radial lining up of interturn gaps?

The connector used in the joints with a wall thickness equal to the diameter of the strands in the outer layer of the cable conductor is an improvement over present practice. Although we have reduced our wall thickness, still we have been using connectors which are unnecessarily heavy.

How much time is it expected will be required to make repairs in case of failure on such installations in steel pipe?

The half-round copper wire spiraled over the insulation of each conductor will have a voltage induced in it which might cause minute sparks to the steel pipe. Have any test data or calculations been obtained regarding these voltages and their effects?

Regarding the other article, drainage of compound may also be a factor in the compression cable. If cracks should occur in the sheath, there would be no warnings telling the operator that compound is draining from the cable into the pipe. This is important, because loss of pressure in the pipe is not considered hazardous so long as the cable is at elevated temperature. However, if gas has entered the insulation under such conditions, then such operation might produce electric breakdown. The further information on the sample with perforated sheath will be, therefore, of considerable interest.

It should be understood that a lot of questions do not necessarily deprecate a development when it is quite new, as are essentially the matters covered in these three papers.

F. W. Main (The Enfield Cable Works Co., Ltd., Brimsdown, Enfield, Middlesex, England): I am pleased to have the opportunity of contributing to the discussion on the three cable papers read at the Chicago summer convention. I have not had any direct contact with experimental work or experience of any installations of gas-filled cable, as my company has been interested in the development, supply, and successful operation of the compression cable in which a pressure medium, usually gas, although not in actual contact with the dielectric, compresses the dielectric through the medium of the lead sheath which acts as a flexing diaphragm.

The compression cable was formerly known as the pressure cable, being the first cable in commercial use employing gas under pressure; later, cables employing gas, either in the dielectric or in direct contact therewith, were constructed, and the term pressure cable was applied indiscriminately to all cables employing gas, and, therefore, in 1940 the title of compression cable was given to the diaphragm cable to prevent confusion.

I would like to express admiration for the development work carried out in America in connection with the compression and gas-filled cables as indicated by the papers read, and I would also pay tribute to the earlier work published in 1939.

The electrical research work in connection with gas-filled cables has been necessarily involved because of the introduction of gas into the active dielectric hitherto normally consisting only of paper and impregnating oil or compound. In the compression cable, however, no elaborate electrical research has been necessary, as in general terms it is essentially a mass-impregnated straight-type cable. Any developments in the electrical sphere have not been peculiar to the compression cable but have been toward improving, by design and shop processes, features and characteristics that can be, and have been, embodied in general impregnated high-voltage and super-voltage cables.

In the paper by Faucett, Komives, Collins, and Atkinson on the 120-kv compression cable, it is stated that the *HSO* construction has been discontinued abroad. This is not quite correct; although the original compression cable installation, which has been in successful operation in Great Britain since 1932, was of the *HSO* construction, and the subsequent cables in Europe, and more recently in Great Britain, have been of the *HSL* pattern, a cable is being manufactured at the present time of the *HSO* type. This cable is not being drawn into a pipe as in the case of the original British installation, as it is a self-contained type, an outer lead sheath acting as the container for the pressure gas.

As the lead-sheath diaphragm in contact with the dielectric breathes in sympathy with the fluctuations of cable temperature, it was considered by some engineers that the lead sheaths of an *SL* three-core cable, being smaller in diameter than the sheath of a three-core screened cable, would be more satisfactory; any fears, however, of a sound lead sheath not giving satisfaction in service have been dispelled by tests made in Europe and now confirmed by experiments carried out in America as described in the paper. The *SL* type has also met with favor, as jointing in some respects is simplified, but this is not peculiar to compression cables.

The relative merits of the pipe-line and self-contained types of compression cable cannot be dismissed in a sentence; local conditions or even prejudice may affect a decision. Obviously, the pipe-line type affords the greater mechanical protection, and the cable can be manufactured in longer lengths; the pipe-line can be installed at a convenient time and the cable drawn in subsequently; the compression cable can be drawn into an existing pipe provided the line in gastight, and, further, if there be sufficient clearance in an existing pipe-line, the cable in position can be withdrawn and a larger cable substituted to give increased capacity. In Great Britain only one of the compression cable installations is of the pipe-line type.

In the paper reference is made to an additional lead sheath to protect the reinforcement tapes of the self-contained cable from corrosion; this additional sheath is, however, not necessary, as there is a number of finishes available that can be used to give further protection and to render the cable impervious to moisture. For this purpose, unless the purchaser specifies an additional lead sheath, we use a sandwich finish which consists of at least two layers of nonfibrous material such as a rubber-bitumen compound reinforced between the layers with a fabric tape.

Mention is made of a spiral wire between the diaphragm lead and the gas-retaining lead of a self-contained compression cable to ensure a free channel for the pressure gas; this we have found is unnecessary, as we employ a circular gas-retaining sheath over a noncircular lead-sheath diaphragm for both three-core screened and single-core types which automatically provides the necessary space.

It is noted that concrete was applied around the pipe in the trench along the armored section of the experimental line. I assume that there is no significance in applying concrete in this particular section.

The investigation into longitudinal movement, comparing the effect of bound and unbound cores, we find is very interesting, and we would welcome more details.

In the paragraph dealing with cable design, mention is made of impregnated jute fillers laid up with the lead-sheathed cores: we have not found the use of fillers necessary.

We are interested to note that tight lead sleeves have been used successfully in joints, whereas we have favored fluted brass or copper sleeves. We shall be interested to have some notes on the effect of fatigue tests taken, especially in the proximity of the wipes.

With reference to terminals, we have given some attention to the use of a Bakelite tube inside the porcelain as discussed in the paper, but we concluded that the complications involved did not warrant the benefits obtained.

Details of load cycles are given in the paragraph headed "Field Test," but whereas a figure for the copper temperature is given in the paper for the 120-kv gas-filled cable, no figure is given for the compression cable. We anticipate that this would be a lower figure, however, because of the lower thermal resistance of the compression cable compared to that of the gas-filled cable.

With regard to the question of power factor, the figure we obtained for cables manufactured in Great Britain is in the neighborhood of 0.0028–0.0030 at 20 degrees centigrade compared to 0.0040–0.0043 as shown in Figure 7 of the paper.

Coming to the paper on the 120-kv-high-pressure gas-filled cable, there are a number of causes suggested as being contributory to the satisfactory solution of the gas-filled cable. Of the seven causes mentioned, surely five at least have been developed by various cable manufacturers for the improvement of super-voltage cables generally and are, therefore, not peculiar to this one type.

It would appear that in the types of gas-filled cable described too much emphasis is placed on the quality of the compound used to impregnate the paper as, by virtue of the lower permittivity in the gas layers, the stress in the impregnating paper is somewhat relieved through higher stresses being thrown into the gas space.

We shall be interested to have more details of the saturant used. A viscosity figure of 3,000 is given for the compound used for preimpregnating the paper for the gas-filled cable; we wonder whether this figure should be 300. It is noted that in Mr. Shanklin's paper the optimum viscosity of compound used is given as 100; we use a mineral-oil-base compound having a viscosity figure of the order of 285 Saybolt.

In Figures 14 and 15 impulse strength values of the *SMD* cables are given and compared with results of certain other cables. One questions whether the other cables were manufactured during the same period as the *SMD* cable and whether the same improved constructional features were common to all, for it does not seem reasonable to anticipate that a dielectric containing gas, even if under pressure and having an uneven stress curve through the whole dielectric, will have impulse values higher than the more homogeneous oil-filled or compression cable, or even a new well-made solid cable.

It is not surprising to learn that the factory shops, where the gas-filled-cable cores are paper-lapped and laid up, are controlled for humidity. This appears very necessary as the paper is preimpregnated, but these precautions are not essential in the manufacture of compression cable.

The lead sheath of the *SMD* cable and the other gas-filled cables is removed on site, and it is found that depreciation takes place in the cable during the process; this is to be expected, however, as it is certainly a delicate operation and must discount to some extent meticulous care taken during manufacture.

In the paper it is stated that the minimum safe-operating gas pressure for the cable is approximately 100 pounds per square inch; yet earlier, it is stated that the electrical tests which, of course, would be at a voltage higher than the working voltage, should be applied to the cable with only 30 pounds per square inch of nitrogen pressure.

Reference is made in the paper to tests for radial power factor and results are given in Figure 10. There is a difference, however, in the radial power-factor curve for mass-impregnated paper as against a true curve for gas-filled cable; the curve given for gas-filled cable only shows power factor in the papers and gives no indication of the effect of the gas space on the shape of the curve through the whole dielectric. One wonders what the shape of the radial power-factor curve would be if the humidity in the shops during manufacture were not controlled.

Unless we misinterpret the authors' statements, we are unable to agree with the inferences given as drawn from Figure 8. The stress in the paper of the insulation is reduced only because the distribution of stress throughout the dielectric is distorted since the permittivity of the gas spaces is of the order of one third of the value of the impregnated paper, the stress in the spaces being correspondingly higher than that in the paper. Therefore, the electrical strength of the impregnated paper cannot be used to full advantage; otherwise the gas spaces would be overstressed, and destructive ionization result.

One can appreciate the usefulness of the low-pressure gas-filled cable, as described in the paper by Mr. Shanklin, for voltages between 30 and 50 kv, where a high breakdown stress is not so necessary as with the higher voltages. As the gas is only at a low pressure, a reinforcement is not necessary, and consequently there is a reduction in cost and diameter, the latter being important in situations where space is at a premium, but the position is not so clear for higher-voltage cables.

It is very surprising to note that, after the cable has been drained, the finished product was almost as well filled as a solid type cable. This is contrary to our own experience.

The authors express doubt as to the mechanical soundness of employing a reinforced lead sheath to contain a gas pressure of 200 pounds per square inch. This, however, is standard practice in England for self-contained compression cable and has given complete satisfaction.

A statement made concerning the effect upon power factor of exposure to air can be appreciated.

While the difficulties of stripping the lead sheath from a gas-filled cable on site as it is drawn into the steel pipe line have been somewhat overcome, it would seem more logical to draw a compression cable into the pipe line and avoid this stripping operation with attendant risks, and at the best, a certain amount of depreciation. It is admitted that a compression cable has a higher factor of safety, and, in the event of the lead sheath diaphragm of the compression cable being damaged, the cable would then automatically become practically a gas-filled cable without the costly complications involved during the installation of such cables.

G. B. Shanklin: In slightly different forms the same two questions have been raised by Halperin, Komives, Del Mar, and Hickernell. In effect, these two questions are:

(a). Why does low-pressure gas-filled cable show more increase in dielectric power factor on overvoltage load-cycle tests than does the best modern type of solid cable, and what significance does this have as related to stability in service?

(b). Why is average stress used rather than maximum stress in the design of mass-impregnated gas-filled cable, and why do strand shielding and extra thin paper tape appear to show no appreciable benefit on comparative load-cycle tests?

Initially and in the absence of ionization the dielectric power factor of both types is the same. The increased power factor of low-pressure gas-filled cable on load-cycle aging tests is merely due to the fact that this type of cable has a greater volume of gas space to be ionized, and the signs of ionization, accordingly, show up more clearly when the cable is subjected to an overvoltage stress where severe ionization can take place. This has no significance at all as related to stability in service. The chief goal of service stability is uniform control of ionization and prevention of a cumulative action. Gas-filled cable gives this through pressure control. That is why laboratory tests on short lengths can be made to simulate closely service conditions and furnish a reliable yardstick for design work. Solid-type cable lacks this uniformity and close control under service conditions, requiring heavier wall of insulation as a protection against localized cumulative weaknesses.

Strand shielding is, in effect, a method of carrying stress grading of cable insulation a step further, to better equalize stressing throughout the insulation wall. It is effective only when the inner layers of insulation are of equal or lesser strength than the outer layers. It is not effective when the inner layers are somewhat stronger than the outer. The maximum-stress theory is governed by the same conditions and does not hold when the successive layers of insulation in an outward direction are of decreasing strength.

When a mass-impregnated gas-filled cable is drained of surplus compound, the laws of capillary attraction are followed. The outer layers of insulation are drained the most, and the largest void spaces occur here. The inner layers of insulation are drained the least, and the void spaces in this location are of minimum size. As a result, ionization either starts in the outer layers first or occurs uniformly throughout the whole insulation wall. It never concentrates in the inner layers alone.

To sum the matter up, mass-impregnated gas-filled cable already has, in effect, automatic grading that counterbalances the higher stress near the conductor. For this reason, any further attempt to grade, such as by use of strand shielding or extra thin paper in the inner zone, does not accomplish so much as would be expected. The use of average stress rather than maximum stress in design work is explained on the same basis.

It should be emphasized that these conditions do not hold for preimpregnated gas-filled cable, where the region near the conductor is the critical zone, and means of improving grading are necessary.

In comparing solid and gas-filled types of cable Mr. Halperin stated that if a man drove a steel point into a cable you would get a signal in the regular manner with either type. He means, of course, that this would happen if the damage were sufficient to immediately cause short circuit. A few months ago a man did drive a steel point into a gas-filled cable, but only the sheath and outer wrappings were damaged. After the gas-pressure relay signal, the damage was located and repaired within a short time without service interference. Service records show that the large majority of failures in solid-type cable are due to sheath damage or leakage from various causes. It is reasonable to assume that gas pressure supervision will largely eliminate such interruptions to service and, so far, field experience has confirmed this.

Mr. Del Mar expresses the belief that the highest possible viscosity of impregnating compound is desirable. In the case of preimpregnated gas-filled cable I readily agree. This type of cable would not work well without extreme viscosity of impregnating compound. In the case of mass-impregnated gas-filled cable some years of experience has demonstrated that lesser viscosity of impregnating compound gives the best results. It is important to obtain the best possible initial impregnation of the heavy wall of insulation. Drainage of surplus compound and capillary balance will then give the right degree of automatic grading, as previously described. These results cannot be accomplished so well with extreme viscosity of compound.

Referring to the medium-pressure system, Mr. Komives is correct in stating that high creep-strength sheath is a new development and needs to be proved. We can only say that this work is progressing well and looks promising. Double-reinforced sheath at 30 pounds per square inch, however, is proved thoroughly in service, and with present quality of sheath the pressure can be increased, provided it is within safe strength limits of the lead wipes. A sudden jump to 200 pounds per square inch, as some propose, does seem, however, to be out of proportion.

Dr. Whitehead asks about residual ionization in gas-filled cable. All of the evidence we have been able to obtain indicates there is none of noticeable effect. Long-time load-cycle tests are always run at an overvoltage sufficiently high to produce pronounced ionization and are of no direct value in clearing up this point. A study of the charts will show that, at each voltage step before ultimate breakdown, ionization is extinguished, and the cable stabilizes because of self-healing action. This would indicate that ionization in this cable is retrogressive and that residual or incipient ionization cannot be sustained. Actual field experience is more conclusive. Gas-filled cable operates at a voltage stress only a little below the ionization-starting voltage stress, as represented in Figure 5 of my paper. This must mean that during switching surges and other overvoltage transients there are at least brief flashes of ionization discharge in the cable. These discharges must be immediately extinguished or, if sustained, must be of negligible effect, because no evidence of ionization deterioration has yet been found in service.

Whether or not Doctor Wiseman's belief that pipe cable systems are not limited to open ground but are comparable to the usual flexible duct system under paved streets is something that will have to be proved by experience. It all depends upon how many leaks are going to occur in the pipe systems and how much tearing up of the streets will be necessary in locating and repairing them. If the means being taken to prevent such trouble succeed, then Dr. Wiseman is correct.

His claim that, for a given amount of power to be transmitted, Oilostatic cable should have a conductor size equal to or smaller than that required for single-conductor oil-filled cable in ducts is based on his well-known method of calculating current-carrying capacity. To accomplish these results Doctor Wiseman uses a soil thermal resistivity of 80 net in his calculations for Oilostatic cable. Most of the rest of us use the more conservative and generally accepted standard value of 120 net, corresponding to a moisture content in the order of five to eight per cent. Only a porous soil completely saturated with water could show a thermal resistivity as low as 80 net. It stands to reason, then, that the relative carrying capacities of the two types of cable are not directly comparable on Dr. Wiseman's basis.

I. T. Faucett, L. I. Komives, H. W. Collins, and R. W. Atkinson: The authors are very much pleased with the interest shown in these two papers and wish to thank the many engineers who have submitted discussions. For the most part these discussions either contribute or ask for additional information. A few are somewhat critical of one or the other of the two types of cable which we have described. Mr. Wyatt wonders why preference could have been denied the compression cable, and Mr. Shanklin fails to see how there could have been any doubt in selecting the gas-pressure cable. It is significant that each of these discussers has shown so much confidence in the type of cable of which he has previously been best informed. Our own very complete investigation in the laboratory and in the field of *both* types gives us confidence in *both* and indicates that satisfactory results are to be expected from either.

In his approach to the high gas-pressure cable Mr. Shanklin has a different viewpoint, concerning several aspects, than have the writers. Mr. Del Mar has summarized these differences very conveniently and has also declared his agreement with the writers in most cases. Mr. Del Mar's tabulation makes a very convenient basis for this discussion.

The first two points may be discussed together. Shanklin uses a much lower-viscosity saturant than the writers and expresses concern about the exposure to the air occasioned by the use of preimpregnated paper. He also states that he has tried high-viscosity compound and has found it apparently inferior. It seems that the difference between Shanklin's results and ours is not wholly in the viscosity. For instance, Figure 6 in his paper, which he considers typical, shows that the power factor of his cable below the ionization voltage at 80 degrees centigrade increases rather quickly to three per cent and then drops to around two per cent, remaining at that value for the duration of the test. On the other hand, the power factor of our insulation is less than one half of one per cent from 40 to 100 degrees centigrade and has shown no change as a result of any of the testing. Mr. Shanklin's discouraging results tend to confirm our contention that our very good results are due to other good qualities in the saturant in addition to its high viscosity.

Mr. Shanklin grants theoretical advantages in the use of thin tape but says that thin tape is more expensive and difficult to apply, tends to wrinkle more, and will not withstand bending well. He apparently concludes that these difficulties cannot be overcome. It seems likely that this is the reason for his failure to obtain good results with thin tape. In our cables we overcame these difficulties by combining a number of means to accomplish that end. We used a superdense paper that had not been previously available in such thin tape. Because of the great stiffness and smoothness of this paper, it slides instead of wrinkling during bending of the cable. This sliding is also facilitated by the lubrication given by the presaturation, as well as by use of proper amount of tension during taping. Finally, the amount of thin paper was limited to that required for electrical reasons. In the region of lower stress, where the mechanical requirements are more severe, successively thicker tapes were used. Not merely were we able to increase the electrical strength of this type of cable with such tape, but we have accomplished a similar result in other types. Beaver has also accomplished important gains with thin tapes of graded thickness, even without the special superdense paper, but he too has had the benefit of presaturation. The experimental high-stress 132-kv cable with only 386 mils of insulation which we manufactured for the Commonwealth Edison Company in 1929 and which has been reported on by Mr. Halperin[1] was made with three-mil paper tapes. This cable showed an exceptionally fine record for the $8^{1}/_{2}$ years net time of testing after which impulse-strength measurements were made on the cable by the Detroit Edison Company. Maximum impulse stress values of 3,250 and 3,450 volts per mil were obtained with corresponding average stress values of 1,890 and 2,010 volts per mil.

The case for strand shielding is similar to that for tape thickness. For many years the theoretical advantages have been recognized. Beaver and others in Europe have found practical advantage for various types of cable. In two papers in 1940 we pointed out important advantages of strand shielding in solid-type cables and we have been able to demonstrate important values in other types. It may be remarked that it is not improbable that the gain occurs more from the shielding from stress of the nonsolid dielectric between strands than from the reduction of stress that is discussed in the classical theories. At any rate the remarkably high dielectric strength for surges which has been obtained on shielded high-gas-pressure cable seems explainable only if the strand shielding is credited with a very substantial benefit.

Mr. Shanklin would like to know, on purely technical grounds, why preimpregnated paper was selected in preference to mass-impregnated paper. The answer is simply that a very high-viscosity compound was essential in order to prevent migration, and the only means by which it was possible to impregnate the tapes thoroughly with such a compound was by the preimpregnation of the tapes. Thus, from the very important standpoint of compound migration, this produces a cable far superior to that which can be achieved with the lower-viscosity compound which must be used with mass impregnation. At the same time superior electrical properties—low power factor and high stability—have been obtained by the use of this compound as shown by the test data reported in the paper.

Mr. Shanklin offers his own answer to his above question by saying: "A comparison of initial ionization-starting voltage stress as a function of gas pressure tells the whole story," and states that such a comparison is given in Figure 6 of his discussion. This comparison is of sufficient interest to merit careful study. In his present paper, Mr. Shanklin says that additional data obtained since the first paper allow a more accurate construction of this curve than before, and it is noted that the values now given are over ten per cent higher than in the old curve. Mr. Shanklin gives all his data in terms of average stress. As recalled to us by Mr. Del Mar, Mr. Shanklin grants: "It is better to deal with maximum voltage stress than average," but pleads the greater convenience of the use of average stress. It is allowable to serve this convenience only when it does not lead one astray by provoking comparisons under conditions where the ratio of maximum and average stress differ so greatly as to distort the comparison. Since Mr. Shanklin has described curve *A* in his paper just referred to as "a representative average of all tests on single- and three-conductor gas-pressure cable," we may suppose that there was no distortion of the comparisons of his own cable by his basis of comparison. In order, therefore, to compare his tests with others on the basis of actual maximum stress, we have determined the actual maximum stresses for one of the cables which he used and on which he has given dimensions. This cable is a single conductor 400,000-circular-mil stranded conductor with one-half-inch hollow core, 300 mils insulation, the stress being calculated by application of the ordinary logarithmic formula from the average stress as given on curve *A*. Likewise the maximum stress was determined for the cable represented by curve *B*. Thus it is found that, at 200 pounds pressure—where Mr. Shanklin states that there is practically no difference between the two types—the *maximum* stress for initial ionization is 300 volts per mil for our preimpregnated cable, as compared with only 236 volts per mil for his mass-impregnated cable. In other words, a 27 per cent higher ionization voltage for preimpregnated paper certainly justifies its selection.

This difference is emphasized still more when it is remembered that the preimpregnated cable on which he has made the comparison was drained after being maintained at a temperature of 100 degrees centigrade throughout its cross section for 65 days in an inverted *U* position. His 1939 paper indicates that curve *A* was based on drainage from load cycles reaching a maximum temperature of about 60 degrees centigrade "for a number of days." Thus, if we take Mr. Shanklin's suggested comparison of ionization tests on a comparable basis of dimensions and conditions, the results show another very effective reason for choosing the type of insulation we did.

It is rather important to discuss certain comments on test methods and significance of tests as made by Mr. Shanklin. He indicates that we should have supplied test data showing load-cycle endurance runs of the type which he has used and which he has found to stabilize his cable. However, as will be shown, the tests and criteria which we have reported have been much more severe and searching than those used by Mr. Shanklin. Since Mr. Shanklin's data for high gas pressure is limited to initial ionization measurements, we must perforce analyze his comments in the light of his data obtained at low pressure. He mentions a certain cable showing little ionization at 77 volts per mil which he tested at various voltages from 85 volts per mil to 120 volts per mil. The cable "stabilized" at each successive step except the last where "the stress was too high for the same kind of stabilization to take place, and failure occurred in a few days." He names an operating stress of 65 volts per mil for this cable and believes that the experience with similar cable operating at this stress indicates "that there is a good margin of safety and some latitude for increasing present working voltage stress." Moreover, as pointed out by Mr. Del Mar, he makes no distinction between cables having different ratios of maximum to average stress, and thus his recommendation, based on average stress, will lead to operation at considerably higher maximum stress on some other cables than on this one cable. In any event the average operating stress which he recommends is 54 per cent of that at which his stabilized cable sample breaks down "in a few days." The cable at Detroit operates at 70 kv, or only 45 per cent of the 155 kv which is withstood indefinitely by most of the samples, or about 50 per cent of the 140 kv required to break down in 120 hours the sample drained at 100 degrees centigrade for 44 days. This serves to emphasize the effectiveness of the test procedure which we have used and to show that it is even more searching than his and that the rating of cable in this way is considerably more conservative than would be obtained by following his program and criteria.

Mr. Shanklin states in reference to gas pressure cable, "We started initial development work in 1932, the principles of which are described in United States patent 1,991,230 filed in July 1933." Our own much earlier work in this connection was disclosed in United States patents 1,524,124, 1,541,937, and 1,651,590 the first of which was filed in 1920 and granted in 1925. These patents have been recognized as basic for gas-filled cable (see letter by P. V. Hunter in *The Electrician* December 21, 1934 and article by C. Turnbull in *Electrical Review*, page 425, 1936).

Mr. Del Mar suggests that the high-pressure gas-filled pipe-type cable system is virtually the Oilostatic system without the oil and oil-pressure accessories and therefore should be interesting from a first-cost standpoint. We feel that the *maintenance* cost on this type of cable will be equally interesting.

In Mr. Del Mar's discussion on compression-type cable it is intimated that Detroit had taken on the combined difficulties of the pipe-type cable and those of the duct-type cable, whereas actually, the commercial line as installed in Detroit cannot have any of the more common troubles of the duct-type cables, since these cables have no lead sheath. Even though the compression-type cable with its lead sheath were used, the pipe would eliminate the major causes for faults occurring on duct-type cable, namely, external mechanical damage, sheath failures in manholes and at duct edges caused by cable movement, and failure from sheath corrosion.

Del Mar and Hickernell have commented on our statement that the power-factor tests on the installed cable, which were not made above 28 degrees centigrade "show no evidence of deterioration." We quite agree that tests at this temperature are not sensitive to some types of changes that are observable in tests made at higher temperatures. However, some types of change are shown more readily at low temperature than high, and, moreover, if there are changes at high temperatures, these are usually accompanied, as admitted by these discussers, by significant changes at lower temperature. Moreover, abundant information on the stability of the high-temperature power factor is given by the laboratory tests which are reported. Figure 10 shows that there is no change in the radial power factor at 80 degrees centigrade after aging for 113 days at a temperature of 100 degrees centigrade throughout the cable cross section. At the time of this writing, there is still no change in the over-all 100 degree centigrade power factor after aging for over nine months at 100 degrees centigrade. Another sample reported was tested at 75 degrees centigrade throughout its cross section with a total time of heating of 1,250 hours and with voltage applied for 530 of these hours of from 1.7 to 2.4 times the rated voltage, and it showed the power factor to be constant throughout the test at a value of 0.3 per cent. The paper by C. J. Beaver and E. O. Davey gives data on cyclic aging tests made on this type of insulation and reports that no sign of ionization nor deterioration could be detected from such tests. The field measurements serve thus as a helpful supplement to the ample data showing the high stability of this cable. These data on the field installation will ultimately be supplemented by measurements at high temperature.

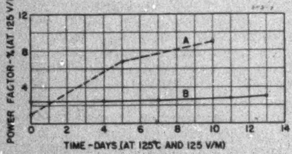


Figure 7. Power-factor aging characteristics metallic shielded conductor versus CB shielded conductor

A—*CB* cable model data from Rosch[2]

B—Commercial three-conductor cable with metal-backed paper conductor shield

Mr. Hickernell makes the comment that all that is needed to make this high-pressure gas-filled cable a truly up-to-date design would be to incorporate Rosch's carbon-black protection. Rosch has urged the value of carbon black as a means of maintaining original low power factor. However, the test data reported for the *SMD* cable show remarkable stability. Original low values of power factor have been maintained throughout the various long-time high-stress and high-temperature tests, and including long-time high-temperature tests conducted at stresses as high as 250 volts per mil. This may be compared with the data for cable "protected with carbon-black paper" shown in curve *A*, Figure 7, taken from Figure 3 in Rosch's paper.[2] This cable during ten days' testing at 125 degrees centigrade at a stress of 125 volts per mil increased in power factor at 125 degrees centigrade from the very low value of about one per cent at the beginning to about nine per cent after ten days. This may be compared with curve *B* of Figure 1 which shows data which we have obtained on solid-type cable with metallized paper shielding maintained at 125 degrees centigrade and 125 volts per mil for 13 days. It will be noted that although the initial power factor of this sample was nearly 2.5 per cent at 125 degrees centigrade, the power factor after 13 days was less than three per cent, showing incomparably greater stability than Rosch's carbon-black sample.

In regard to impulse strength, Mr. Hickernell refers to the data in the reports by L. I. Komives and G. B. McCabe and makes the statement that, "These data indicate the highest impulse strength was obtained with type-*CB* construction." There is nothing in either of these two reports that would justify any such statement. In Mr. Komives' report the only type of strand shielding tested was that having *CB* construction, and therefore it is impossible to make any comparison between it and cable having metallized-paper conductor shielding. In Mr. McCabe's Edison Electric Institute report, the average values reported did show a slight advantage in favor of *CB*-type cable, 1,870 volts per mil average stress as against 1,833 volts per mil for cable using this type of metallized paper. However, Mr. Hickernell evidently overlooks the fact that the value of 1,833 volts per mil is lower, because it includes terminal failures (not cable failures) on two of the three conductors tested and also overlooks the author's definite statement to this effect, that "It should be borne in mind that since conductors 1 and 2 failed in the terminals, the surge strength of the insulation of these two conductors was somewhat higher than the recorded values probably equal to or higher than the maximum value of average stress attained in same 18" (with *CB* tape)."

Another point of superiority of the metallized tape used for insulation shielding is that it proved to be a very effective moisture barrier. Two samples of unsheathed *SMD* cable exposed to humid atmosphere for approximately two months showed no increase in moisture content of the insulation. The physical properties of carbon black are such that it is improbable that it would provide this desirable moisture barrier as does the metallized tapes.

It is difficult to understand the basis for conclusions drawn by Mr. Hickernell from the report of F. Farmer on cable in vertical runs. The only comparison we can draw concerning compounds in this report is that the compounds which showed low migration had higher viscosities at all temperatures than the compounds which gave trouble from migration. In this reference it should be noted that those cables made in 1915 to 1920 and impregnated with a so-called nonmigrating compound consisting of petroleum grease and rosin showed extremely low migration, despite the fact that several of them carried 1918 war loads which would produce temperatures in excess of the melting point of about 52–53 degrees centigrade for such compounds. The Saybolt Universal viscosity of such nonmigrating compound is less than 1,500 seconds at 60 degrees centigrade, whereas the viscosity of *SMD* compound is 40,000 seconds at 60 degrees centigrade. At the maximum normal operating temperature of 70 degrees centigrade, the *SMD* compound has a viscosity of approximately 20,000 seconds, which is in the range that the usual solid-type cable oils will have at temperatures of 20 degrees centigrade or below. Thus, there is no more reason to worry about migration in this gas-filled cable at 70 degrees centigrade than there would be with solid type cable at 20 degrees centigrade.

Mr. Halperin has asked what temperatures are considered permissible for the gas-filled and compression cables. It is recommended that the same permissible temperatures that have been adopted for oil-filled cable be used for these two types of cable. In regard to variation in gas pressure, the maximum range of gas pressure caused by temperature changes in operation will occur between the conditions of a sustained maximum load on the feeder at the time of maximum earth temperature and the removal of the feeder from service for an appreciable period at the time of minimum earth temperature. If this minimum pressure is made 200 pounds per square inch, the maximum pressure will be about 230 pounds per square inch for the *SMD* installation. The more probable range of pressure that will occur in normal operation will be considerably less than the above maximum. Mr. Halperin draws attention to the fact that the five types of cable included in Figures 14 and 15 cover a considerable range in degree of impregnation and should therefore show some differences in impulse strength. The data presented in these figures, except for the *SMD* type, are not new but have been taken from published data. The high values of impulse strength for the *SMD*-type insulation tested under high gas pressure, as compared to previous measurements on drained cable, may be attributed in part to the high dielectric strength of gas at this high pressure. As stated by Mr. Shanklin in his discussion, "the electrical strength of nitrogen gas at 200 pounds pressure is almost as great as that of the solid insulation." Also it is very probable that conductor shielding and the thin graded tapes contributed in securing these high impulse values for the *SMD*-type cable. We are not familiar with the data Mr. Halperin has which disagrees with conclusion 5 and wonder if it does not apply to a different type of insulation than the *SMD*-type using high gas pressure, graded tapes, and strand shielding, to which conclusion 5 is intended to apply. Test failures occurring in the regions of wide interturn gaps are probably due to both reasons given by Mr. Halperin. The half-round copper wire is in direct contact with the steel pipe and is solidly grounded to the pipe at all joints so that no appreciable voltage will exist between turns or from the wire to pipe, and there will be no tendency for sparking.

Mr. Wyatt refers to Figure 7 in the compressor-cable paper and states that the values of "power factors at room temperature or below up to 0.5 per cent appear rather high." He goes on to speak of having made compression cables "the 20 degrees centigrade and 60 degrees centigrade power factors of which were as low as 0.20 per cent and never higher than 0.30 per cent." We feel sure that Mr. Wyatt has overlooked the fact that with impregnated paper of very low loss, the temperature for minimum power factor is well above room temperature and that the power factor at temperatures *well below* room temperatures as given in Figure 7 will be greater than at ordinary room temperatures.

We know of no lower accurately determined values of power factor on paper cable than those reported in Doctor Whitehead's extended series of carefully controlled measurements of cable insulation. Thus, though Mr. Wyatt states that none of his values exceeded 0.30 per cent, none of Doctor Whitehead's values at 30 degrees centigrade or below are as *low* as 0.30 per cent. Doctor Whitehead's lowest values were obtained with oil of extremely low power factor, the oil being very fluid and the paper of low specific gravity as compared with usual cable practice. It seems likely from various collateral information, that the lowest of Doctor Whitehead's values represent a condition substantially as low as have been obtained with cable paper and any saturant. We suspect that, if Mr. Wyatt will investigate carefully, he will find that the values he mentions apply not at 20 degrees centigrade and below but at the higher temperature of 60 degrees centigrade or more, where the power factor should be considerably lower than 20 degrees centigrade.

From Mr. Wyatt's statement that: "Although the authors use thin paper in the conductor region to keep down butt spaces, wafers show that such a cable, after bending, pulling, and installing, will have some gas spaces of 18 mils and even 20 mils radial dimensions", it is not clear whether this is a deduction or whether he has prepared or secured a wafer that seems to indicate that conclusion. Also it seems to imply definitely that such spaces are to be expected even in the region of thin tape. We have made a good many wafers and believe that this statement is entirely unrepresentative of the cable that is installed in the commercial line at Detroit. Our wafers, made from samples of this cable, show that the maximum spaces that were found are quite closely equal to the tape thickness. It is most significant, however, that the normal spaces for the $2^1/_2$-mil paper are just about one-half the normal spaces for the five-mil paper and that likewise the maximum spaces for the thin paper are also half as large. The wafers show this advantage of the thin paper very clearly. It is recognized that in long sections of cable there is some probability of spaces occurring that will be equal to twice the tape thickness but extremely improbable that spaces appreciably greater than this will occur in modern cable.

It is natural that such a system as described in each of these two papers did not grow full-fledged at one time and that there are some parts which are not entirely new. The authors have been concerned primarily with giving engineering information concerning a successful system which it has been their job to improve, rather than with writing a bibliography, but they are pleased to have Mr. Wyatt express his opinion as to where certain credit should be given.

We are very pleased that Mr. Thomas has mentioned his sheath-compensated cable as it has many things in common with the compression type of cable. In this connection, it is interesting to note that some of this cable which we manufactured in 1935 is still operating with a record of no inherent faults to date. In this design vulcanized rubber tapes are applied over the lead sheath at a tension of 1,000 pounds per square inch. The elasticity of these tapes causes the lead sheath to compensate for the internal expansion and contraction caused by load changes and thus prevents void formation.

In reply to Mr. Thomas' questions, laboratory tests show that fatigue strength of the lead cable sheath when subjected to vibration or bending is considerably increased by the elimination of oxygen from contact with the sheath surface. In reference to the countersunk connector, this is of the soldered type with two short slots on the top for receiving solder. The outside diameter is the same as that of the cable conductor. In order to apply the connector, the outer layer of conductor strands on each of the cable ends is cut back for a distance of half the connector length. When the connector is wiped in place, these strands are flush with the connector and joined to it. In Figure 5 of the paper on compression cable, the step on which the joint sleeve ends is the lead sheath of the cable. The next step is the termination of the sheath-reinforcing tapes. No void space is left under the joint sleeve. Varnished cambric tape is used to build up the diameter of the sloping ends over the shielding braid to a dimension equal to the inside diameter of the lead sleeve. This diameter is carefully checked in order to secure a tight uniform fit along the full length of the sleeve.

Approximately 60 per cent of the commercial line was installed under paved city streets. About 50 per cent of this line was installed under streets using the heavy paving, 10 per cent under streets using light paving, and the remaining 40 per cent installed where no paving existed. A cost estimate made of the circuit indicated that the total cost to install oil-filled cable in conduit with accessories would be about 20 per cent higher than the cost of a high-pressure gas-filled pipe-type cable installation.

The paragraph questioned by Doctor Whitehead does not make the conclusion that the stress in the gas spaces is less than that in the other types of insulation. This paragraph is intended to point out that to whatever extent presence of gas reduces the over-all dielectric constant, the stresses in the oil and paper are, thereby, correspondingly reduced. Thus at any given applied voltage, the stress in the oil and paper will be (slightly) lower than on other types of cable such as oil-filled, Oilostatic, or compression cable. For all voltages at which there is no ionization in the gas space the electrical stress in the oil and paper will have no effect not found at least equally in these other types; or as stated in the paper, stress below the ionization voltage in the gas should have no effect on the insulation.

It has been pointed out by Doctor Whitehead and others that there is undoubtedly some ionization at voltages below the level necessary for detection by power-factor measurements. It will be interesting to study this by the more sensitive type of measurement such as used by Doctor Whitehead and by Professor Paine. However the shape of the voltage-time curve of Figure 13 shows that no harmful ionization occurs appreciably below the voltage at which we have measured ionization. Above the critical voltage indicated by the short horizontal section of the curve, the life decreases rapidly with increasing voltage. Below this critical stress, application of voltage has no effect on the life of the cable. An additional point on this curve has now been secured—one cable length has failed after a life of over 1,600 hours at an average voltage of 155 kv. This test was begun at 148 kv, voltage being raised uniformly, ending at 163 kv. While there is some variation from one length to another as shown by some scattering of points on the curve, a constant voltage that does not produce failure in 100 or 200 hours apparently will never produce failure.

It is obvious that, above the critical stress, the extent and destructiveness of the ionization increase rapidly with increasing voltage; the converse is also true, the extent and destructiveness rapidly disappear as the voltage is reduced below the critical value. Since there is, of course, some longitudinal nonuniformity, the critical stress on some specimens will be lower than on others and will be lower than for the average curve that is drawn. From the evidence from severely handled specimens, the minimum critical value appears to be not very far below 140 kv, or twice the working pressure of this cable. Thus, as far as concerns the normal 60-cycle voltage, there is a wide margin permitting future increases of stress by reduction of insulation thickness. This margin is indeed greater than the very considerable margin that exists in the surge strength so that the ultimate reduction in insulation thickness will doubtless be determined by surge strength rather than by 60-cycle strength.

Doctor Wiseman considers the restraining force measurements on the 50-foot sample as inconclusive because of the short length. It is true that the section was short as compared with actual sections, but still it was of sufficient length to permit the cable to take freely the same configuration in absorbing the longitudinal expansion as would occur on a long section, and therefore it is felt that these values are representative of the maximum values of thrust that may be encountered on an actual section. At the most the criticism of the test is merely to the effect that the small easily handled thrust measured may be even less in an actual installation. In calling attention to the coefficient of friction for *SMD*-type cable when pulled into steel pipe, Doctor Wiseman states that 0.37 is very good for dry pipe, but for Oilostatic, where the cable oil also acted as a lubricant, they got a value of 0.26. In the paper it was stated that 0.37 was the maximum value (not average value) that was reached for any section in the seven-mile line including the sections with bends. The average value was considerably below this. Since the coefficient of friction inherently varies over such a wide range, depending on many factors that cannot be evaluated exactly, the average value was not given, since it is felt that without knowing these controlling factors the average value of this coefficient is of doubtful significance. It should be pointed out that petrolatum was used as a lubricant and was applied over the half-round open spiral wires before the lead sheath was applied. Since petrolatum is a better lubricant than cable oil, the *SMD*-type cable can be expected to have a lower coefficient of friction than Oilostatic cable, since otherwise the external structures are similar, and therefore the maximum pulling length would be at least equal to Oilostatic cable.

It is very helpful to have Mr. Main's supplemental information concerning compression cable. His confidence in the value and reliability of this type of cable is based upon the very practical ground of successful production and operation of a considerable amount of it. In addition to his general comments, he tells of significant modifications of earlier designs. These should be especially helpful wherever the self-contained type is applicable.

Mr. Main grants certain advantages of the pipe-type cable but evidently is more impressed by the merits of the self-contained type. For smaller amounts of power than being carried at Detroit, these merits will often predominate, but where large conductor sizes are required as is generally the case in this country, the pipe-type system proves to be the more practical.

We are glad to have Mr. Main's figures of power factor at 20 degrees centigrade for the British-made cables and note that these are somewhat more than 0.1 per cent lower than the values given in our Figure 7 for the field measurements on the experimental compression cable. Much of this difference is accountable by the fact that density of the paper in the Detroit cable is very high, with a corresponding reduction in the amount of oil. With very low loss oil, such as used in this cable, the power factor of the paper substance is higher than that of the oil. Thus the greater paper content means greater over-all power factor. Any difference that cannot be accounted for in this way is only a fraction of a tenth of one per cent and can be explained by the difference between measurements made in the field as compared with the more accurate factory measurements. Thus Mr. Main checks our values which were questioned by Mr. Wyatt.

There is not space here to discuss fully the practical importance of the high-density paper. It suffices to point out that the large reduction in oil content results in proportionately less volume change with temperature than with lower-density paper. Incidentally, there is also a gain of this sort from the compact strand as compared with conventional. The much wider temperature range thus required for a given volume change is vastly more important than any minor difference in loss due to the difference in power factor.

In referring to the causes "contributory to the satisfactory solution of gas-filled cable," Mr. Main suggests that several of these have been developed by various cable manufacturers for the improvement of super-voltage cables generally. That is fully conceded. Some of these are of course particularly significant for the gas-pressure cable.

Mr. Main suggests that the reduction in stress in the impregnated paper by reason of the lower permittivity of the gas may reduce the emphasis that need be placed upon the quality of the saturant in the gas-pressure cable. We have drawn attention to this reduction of stress but would emphasize that it is purely incidental and actually amounts to only a few per cent. He asks if the viscosity of 3,000 given for this saturant is correct and not wrong by a decimal point. Three thousand voltage is correct, and we are glad to have the emphasis placed upon it that comes from the question. We believe that the properties of the saturant used, including this high viscosity, are very significant.

We quite agree with Mr. Main concerning relative impulse strength of different types of cable. We would be surprised if oil-filled cable made with the same type of paper and with strand shielding would not have as high impulse strength as this cable, although it is to be noted that preimpregnation facilitated the use of very thin tapes without the mechanical difficulties which Mr. Shanklin has found in applying unimpregnated thin tapes.

Mr. Main brings together for comparison our statement that the minimum safe-operating pressure is 100 pounds per square inch and our statement that the cable may be given standard acceptance voltage tests at 30 pounds per square inch. The operating limitation is, of course, conservative. There is, however, no particular advantage in setting a lower limit, since the line and accessories will readily take care of 200 pounds per square inch or more, and the cable will be operated at that pressure. As far as concerns test practice in this country, acceptance tests are made at voltages far above what anyone would consider to be a safe-operating voltage. It is true that this is exaggerated by the suggestion of making a normal test at a pressure lower than suggested for operation. However, it is to be noted that the dielectric strength for a period of a few hours is reduced not more than 10 per cent or 20 per cent at 30 pounds per square inch as compared with 225 pounds per square inch. It is this fact, as well as the results of dissection of tested samples, that warrants the testing for 15 minute periods to be made within the same range as is customary for oil-filled cable.

Mr. Main comments concerning the radial power-factor curves, saying these give no indication of the effect of the gas space on the shape of the curve. We see no reason for this concern; the curve actually obtained explores fully the variations of the impregnated tapes, and this seems to us to complete its purpose. Mr. Main himself recognizes this function when he "wonders what would be the shape of the curve" without humidity control in the factory. Actually these tests show how effectively that control was accomplished, and similar tests were a helpful means in setting up required standards for that control.

Mr. Main brings question as to the meaning of the same paragraph which Doctor Whitehead questioned. Our answer to Doctor Whitehead substantially answers also Mr. Main. The statement we wish to emphasize is a corollary of one Mr. Main makes that "the electrical strength of the paper cannot be used to full advantage; otherwise the gas spaces would be overstressed and destructive ionization result." But this means that if we avoid destructive ionization in the gas spaces we need feel no concern about the electric stress in the oil and paper. This fact is so very obvious that there was a tendency to look for some different and less easily accepted meaning. But this leads directly to the conclusion that the electric stress in the oil and paper in this cable is well below that which long experience with other types of cable has shown to be without harmful effect.

Mr. Main refers more than once to "difficulties" of stripping the sheath; he feels that this involves "costly complications." Before making the experimental installation, this seemed somewhat of a problem, but, by the time of the commercial installation, no problem remained. We can assure Mr. Main that his fear on that score is without foundation. Indeed we are constrained to remark that Mr. Main's comments emphasize our previous remark that both of these systems are good and very practical. Those acquainted with one will be partisan to that while fearing unfamiliar problems of the other. Acquaintance with both emphasizes the value of both.

Mr. Peterson has asked the expected life of the pipe when protected by one half inch Somastic as was applied on the Detroit commercial line. We have not been able to find any case of corrosion or deterioration with this type of protection, and installations have now been in service up to ten years. In regard to expansion, the expansion bend was provided only in the experimental installation and was used only to take care of expansion in the armored compression cable. No expansion provision is necessary for the pipe itself. In the *SMD*-type cable the three conductors have considerable freedom for movement and can absorb the longitudinal thermal expansion without any other special provision. As to the viscosity of the impregnating compound used on the *SMD* cable, it is about 80,000 at 50 degrees centigrade in terms of Saybolt Universal seconds.

No effort was made to determine the moisture content of the soils encountered because of the poor correlation that appears to exist between moisture content and thermal resistivity in an actual installation. This may be attributed in part to the difficulty in securing a moisture-content sample that is representative, as well as to the fact that for the Detroit installation the ground water level has a greater effect on the thermal resistance of the soil. A record was maintained of the rainfall, and it was found that this as such had practically no correlation with the seasonal soil resistivities reported.

Mr. Del Mar has concisely summarized the limited available knowledge on the subject of earth resistivity, and it was because of this inexact knowledge that these earth resistivity measurements were made and presented at this time. Several discussers have asked for more information concerning the thermal-resistivity measurements or concerning the thermal characteristics of the high-pressure pipe-type cable systems, or have asked why more data were not presented on this subject since more were obtained. These tests have not been completed.

The discussions by Meyerhoff, Johansson, Frank, and D'Eustachio, all of whom are associated with two of the authors may be considered as supplementary to the authors' closure.

REFERENCES

1. LOAD RATINGS OF CABLE—I, II, Herman Halperin. AIEE TRANSACTIONS, volume 58, 1939, October section, pages 535–56; volume 61, 1942, pages 930–42.

2. THE TYPE-*CB* IMPREGNATED-PAPER CABLE, Samuel J. Rosch. AIEE TRANSACTIONS, volume 59, 1940, pages 1041–50.

The Dielectric Strength and Life of Impregnated-Paper Insulation—III

Discussion and author's closure of paper 42-92 by J. B. Whitehead, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, August section, pages 618–22.

G. B. Shanklin (General Electric Company Schenectady, N. Y.): There has been a good deal of speculation about Doctor Whitehead's breakdown results being at variance with actual solid-type cable experience, as regards paper density. The following explanation is offered for what it is worth.

Because of entire absence of void formation, Doctor Whitehead's test samples are actually representative of oil-filled cable and not solid-type. The mechanism of breakdown he is studying is, accordingly, applicable in all detail only to oil-filled cable. This is borne out by the fact that his results are in full accord with oil-filled-cable experience.

For times expressed in seconds or fractions of a second (impulse, and so forth), the dielectric strength of oil-filled cable increases with increase of paper density. For times in hours, such as Doctor Whitehead used, the dielectric strength decreases slightly with increase in paper density, just as he found. For times expressed in months or years, change in paper density, over the usual commercial range, has practically no effect on dielectric strength.

Ionization in voids of solid-type insulation can easily change these trends, particularly for the longer periods of time. The action is more complicated and more difficult to reproduce in the laboratory, because it depends on size, quantity, and location of these voids, as well as pressure and numerous other factors. For this reason I suppose we will always have differences of opinion about the effects of paper density in solid-type cable, or in any other type of cable where incipient failure is brought about by gaseous ionization in voids. Personally, I cannot get very excited about it, because the differences are not great in any case.

If Doctor Whitehead needs any support in his belief that Robinson was wrong in concluding that *all* failures are instigated at the conductor surface, I stand ready to give it. Like Doctor Whitehead, I have examined plenty of incipient failures at locations other than the conductor surface, and this was in full-sized cable, some of which had relatively large ratios of maximum to minimum stress.

Too many factors besides quantitative stress are involved for one to say that the location of breakdown in paper-insulated cable follows a simple physical law, based on the geometrical stress field alone.

In overstressed gas-filled cable, for instance, ionization deterioration is usually more pronounced in the outer zone of the insulation thickness rather than the inner zone, and final failure usually starts here. This occurs, because compound drainage is considerably more in this outer zone, and voids are larger.

There are many champions of the maximum-stress theory and some champions of the minimum-stress theory. The fact remains, however, that practical design work in the whole cable industry is based on neither but, instead, is based on average stress. My experience has been that over the usual commercial range of conductor sizes, insulation wall thicknesses, and so forth, average stress expresses practical results better than anything else. This middle of the road course is a safe one to follow in all but extreme and special cases.

William A. Del Mar (Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): Doctor Whitehead's modification of the Robinson coring theory is very interesting.

Like Robinson he finds that initial trouble arises in an oil space and that the oil space involved is one where the greatest electrical stress occurs.

However, because of the circumstance that his insulation is thin compared to his conductor diameter and that his conductor is smooth, he regards it as a matter of chance whether the stress is greatest at the inner surface of the insulation or at some intermediate layer.

Perhaps there is another reason. Reference to P. L. Hoover's 1926 paper shows that the maximum stress at very high stress is not at the conductor surface but within the insulation, and this may influence the results obtained.

Again, Doctor Whitehead agrees with Robinson that failure originates in gas in the oil space, but, whereas Robinson postulates a gas occlusion, Whitehead says that if there is no gas occlusion present, and the stress is sufficiently high, the stress will generate the gas by ionizing the oil.

This immediately suggests two levels of dielectric strength, one applying to practical cables from which complete gas exclusion is impossible, and a higher level applying to completely gas-free laboratory specimens like those of Doctor Whitehead.

I shall try to establish the fact that Doctor Whitehead's specimens fail at a higher stress level than cables and will then try to draw the consequences.

Reading over Doctor Whitehead's three papers on stability of oil-impregnated-paper insulation, I find that all stresses referred to are average stresses and that, where the expression "maximum stress" is used, it means "the maximum or final value of the average stress."

Thus, Figure 1 of the paper gives an average final stress of nearly 600 volts per mil for a specimen made with paper of specific gravity of 0.896 and designated as *B* paper, after being subjected to 13 stages at average stresses starting at 400 volts per mil.

The same Figure 1 gives, for this paper, a life of 53 hours, practically all of which was consumed in the 13 preliminary stages.

We have no exact way to correlate the preliminary steps with the final steps, but we may make an approximation by assuming that each stage causes deterioration at a rate proportional to the 20th power of the stress. If you ask the source of this figure, I can give you nothing better than some test data on oil-filled cables which suggested it. In the case of single-conductor solid-type cable, the index is about 6.

On this basis the preliminary stages are equivalent in the aggregate to about $4^1/_2$ hours at the final stage for all the papers tested.

Returning to the *B* paper, if we plot the original test data with volts per mil as abscissas and hours as ordinates, we obtain a gunshot diagram with average stresses ranging from 581 to 700 volts per mil and lines ranging from 0.2 to 3.83 hours.

If we average these lines, we obtain about 2.3 hours, but this would not be a significant average, as it would not take into account the various stresses at which the individual lives were obtained. This may be allowed for in an approximate way, by the same assumption made above, that is, by assuming the life to be inversely as the 20th power of the voltage and obtaining a weighted average based on that assumption. This gives a life of $3^1/_2$ hours.

If we add this to the $4^1/_2$ hours from the preliminary stages, we obtain, for paper *B* specimens, an empirical life of eight hours at 600 volts per mil.

The nearest comparable cable test data are those from the Association of Edison Illuminating Companies "Time Test," which give failures for single-conductor cables with thin walls at average stresses of about 400 volts per mil after

6 hours at 220 volts per mil
3 hours at 300 volts per mil
3 hours at 350 volts per mil

Table I

Paper Density (Gurley) Seconds	Specific Gravity	(Volts Per Mil) Overlap $33^1/_3$%	$37^1/_2$%
33	0.74	349	342
480	0.78	420	367
3,000	0.94	390	428

In the case of solid-type single-conductor cable, an index of 6, instead of 20, gives a more accurate means of weighting the lines. On this basis, the preliminary stages give the equivalent of about two hours at 400 volts per mil. The tests usually run from one quarter to three quarters of an hour at 400 volts per mil, a fair average being not over one-half hour. This added to the two hours from preliminary stages, give $2^1/_2$ hours at 400 volts per mil.

By this roundabout route we arrive at the following comparison between Doctor Whitehead's specimens and ordinary cables.

	Failure Stress (Average) Volts Per Mil	Time at Failure Stress (Hours)
Doctor Whitehead's specimens	600	8
Cables	400	$2^1/_2$

This seems to establish that Doctor Whitehead's specimens fail at a definitely higher stress level than solid cables, this level being at least 50 per cent greater. If the comparison is made on the basis of final stresses only, Doctor Whitehead's specimens appear to have at least 75 per cent greater dielectric strength.

In the case of failure by generated gas, the stress on the oil is all important, as the generation of gas is directly dependent thereon and becomes violent when this stress is exceeded. Therefore, in the laboratory specimens, the specific inductive capacity of the paper is of prime importance, and a high value works against high dielectric strength, as pointed out by Doctor Whitehead.

Where, however, failure is due to occluded gas, ionization starts at a low stress, whatever the density of the paper, and the stress at failure depends on the ability of the paper structure to prevent spread of the ionization streamers, an ability which increases with increasing density, provided that the tangential path is not weaker than the path through the paper. We have made extensive tests on specimens made purposely with poor impregnation which completely support this view. A progress report on this was presented to the National Research Council's Williamsburg conference in 1941 and referred to by Doctor Whitehead in his paper. Details of this investigation will be offered to the Institute when the exigencies of more important war work permit.

In the meanwhile typical data on poorly impregnated specimens ten tapes thick are offered in Table I for comparison with Doctor Whitehead's.

The voltage was increased by five-minute steps, each step being 20 per cent higher than the preceding one. These were converted to an arbitary equivalent one hour dielectric strength by means of the sixth root law.

The above figures are the average of eight breakdowns. The maximum deviation from the average was 8.9 per cent, and the average deviation 4.5 per cent.

It will be noted that at $37^{1}/_{2}$ per cent overlap, dielectric strength increases with density whereas at $33^{1}/_{3}$ per cent overlap, the relative dielectric strengths of the papers and tangential paths produce a condition wherein a maximum occurs at a medium density.

R. W. Atkinson (General Cable Corporation, Bayonne, N. J.): Doctor Whitehead's studies are leading to a better understanding of the phenomena occurring in dielectrics under high electric stress. The very fact that certain of the conclusions from his tests were originally so unexpected by "dielectricians" greatly emphasizes the importance of these results in our understanding of the effect of electric stresses.

Why some of the relationships differ from those found in commercial cable insulation is not yet obvious, although the explanation may be found among the theories that have been suggested by various people. Since these things are susceptible of effective experimental attack, the explanation will be forthcoming when sufficient effort can be devoted to the subject.

J. B. Whitehead: When our first results on the influence of paper density on dielectric strength were announced, there was considerable disquietude amongst cable engineers who were following closely the progress of our work, and some of whom advocated the use of high-density paper. There was some evidence that the accuracy of the results was questioned. As they were substantiated by further data, cable engineers united in an effort to collect from their own laboratories results of tests in favor of high-density paper. Reports were received from five or six laboratories, but, with one exception, all the tests had been made on continuous sheets of paper and indicated increasing dielectric strength with increasing paper density, in accordance with theory and long-established behavior of other materials. The single exception noted was one impulse-voltage test on each of two cables, one with high density and the other with low density paper—the higher density giving a somewhat higher breakdown strength.

Much of the uncertainty of the situation thus created was due to the impossibility of reconciling our results with the behavior in breakdown tests on solid-type cables; this, in spite of the fact that we have emphasized the absence of free gas in our samples, and that our results, if applied to cable behavior, should be confined to those of oil-filled type.

We have never ventured to suggest the extent to which our results should be considered in cable design, leaving this question to the vastly wider experience of cable engineers. However, in the face of some questions and criticism, we have ventured from time to time to ask for the experimental evidence on cables, which was at variance with our own. None worthy of the name has been forthcoming. Mr. Shanklin goes further than others in stating the results of manufacturers' experience. It would be helpful if he would give some quantitative data substantiating his statements as to the relative influence of paper density in impulse, short-time, and long-time testing, respectively. Requests for quantitative data from those manufacturers who advocate the use of high-density paper have always been met with the response that their experience was of general character over a period of years and that it would be difficult to separate the particular studies which had led to conviction that a denser paper results in higher dielectric strength. Hardly a convincing reply, in view of the number of factors which have entered into the general improvement of cable quality within recent years.

As stated above, it is not our intention to question the experience and practice of cable manufacturers, nor even to suggest the extent to which weight should be given to our results. I believe, however, that there is now no question of the accuracy of our results on the influence of paper density in our type of sample. This means that the influence of the relative values of the dielectric constants of cable tape, and of oil channel or gas space, is such as to throw an increased value of stress into the oil channels and gas spaces, and that this stress increases approximately as the density of the paper. Whether or not this fact shows itself only at relatively high stresses, as in our experiments, as pointed out by Mr. Del Mar, the fact remains that, if failures do originate in oil channels and gas spaces, the use of higher-density paper increases the stresses in these most vulnerable locations. It does not appear to me that the question has been satisfactorily answered, and it is to be hoped that somehow, somewhere, after the present unhappy period, further experiment may be possible for clearing it up.

Load Ratings of Cable—II

Discussion and author's closure of paper 42-111 by Herman Halperin, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, pages 930–42.

G. B. Shanklin (General Electric Company, Schenectady, N. Y.): Mr. Halperin has made another valuable contribution to cable engineering. Systematic operating data of the kind he presents can only be obtained directly in the field by a large, well-organized operating company. This is particularly true of the behavior of cable under heavy load conditions, and it is about this that I wish to comment.

For a number of years some engineers have been advocating rather extreme loads and copper temperatures for power cable, limits that appeared to many of us to be contrary to actual experience. It is interesting to note that, based on the extensive field study he has been making the past two or three years, Mr. Halperin has reached relatively moderate views about normal safe loading. This is summarized in Table VII of his paper, representing the latest maximum copper temperature practice in Chicago. For normal operation of solid-type cable he follows the well-known standard rule of $(90 - E)$ degrees centigrade in all but three instances. At the minimum and maximum voltage ratings he increases copper temperature by five degrees centigrade, while at one intermediate voltage rating he decreases temperature by five degrees centigrade, as compared with the standard rule.

My only disagreement is with the recommended limit of 65 degrees centigrade for 66-kv solid-type cable with solid filled joints. I do not believe there is enough actual field experience to verify this and feel that even the present standard rule limits for 33-kv three-conductor and 66-kv single-conductor cable are too high.

For wartime loading conditions Mr. Halperin is recommending from three degrees centigrade to ten degrees centigrade increase over normal conditions, but emphasizes that increased service failures and shortened life of cable resulting therefrom must be expected and accepted. This, to my mind, is the core of this whole problem of heavy cable loading. It is not a question of obtaining something for nothing but of weighing the gains against the losses.

Mr. Halperin is recommending occasional short-time emergency overloading of five degrees centigrade to 27 degrees centigrade above normal loading and seven degrees centigrade to 35 degrees centigrade above wartime loading. Only in the recent past have we begun to think in terms of a 15 degrees centigrade tolerance for emergency overloading. I suggest that we obtain some experience with this limit before going too far. Mr. Halperin points out that he does not know what might happen to accessories under such extreme temperature ranges, and it is again a question of weighing the gains against the losses.

Mr. Halperin states that in some cases considerable thermal advantage may be gained by making relatively deep installations of underground ducts. This is undoubtedly true and is particularly true of the Chicago district, but it depends upon the permanent underground water level. If this can be reached, there is a material cooling effect on the loaded cables. If not reached, there is practically no difference in cable heating at any depth from $2^1/_2$ to 12 feet, the cooler earth at greater depth being counterbalanced by the higher thermal resistance to earth's surface and other factors. It is for this reason that standard load ratings of cable would be greatly simplified if we standardized on a depth of three feet and earth ambient temperature at that depth, ignoring the actual depth at which the cable is laid. The chances are, in any case, that the depth will vary from one end of a line to the other.

J. B. Whitehead (The Johns Hopkins University, Baltimore, Md.): In reference to Mr. Halperin's paper and to the evidence of discharges from an oil layer between the insulation and the surface of the sheath, contact with both lead and paper of this oil layer of appreciable thickness would certainly result in an increased ionic content of the oil. This would lead to space charge layers at the surfaces of both sheath and insulation with resulting high local stresses leading to local bridges, or local failures, or abnormally high stresses because of surface irregularities.

To refer to Mr. Halperin's paper and to the question of the mechanism of failure, the following may be said: A thorough failure with even a moderate amount of burning is usually sufficient to completely conceal the first cause of failure. Failures in this type of insulation almost invariably originate in an oil channel in which the spark is due to high local stress at the interface between paper and oil. A gas bubble is formed by the initial spark, and cumulative ionization then begins. The consequent burning or boring through the insulation wall is not reflected in a change in the overall power factor of the sample, until the failure is almost complete and is such that a heavy increase in the conduction component of the current may result. These statements are largely based on accounts of many partial failures reported in my paper on today's program.[1]

Will Mr. Halperin please explain the statement that cable samples made with very thin insulation will show more definite deterioration than thicker samples?

REFERENCE

1. THE DIELECTRIC STRENGTH AND LIFE OF IMPREGNATED-PAPER INSULATION, J. B. Whitehead. AIEE TRANSACTIONS, volume 61, 1942, August section, pages 618–22.

L. F. Hickernell (Anaconda Wire and Cable Company, Hastings-on-Hudson, N. Y.): This paper is so complete that it leaves few, if any, important questions to ask the author. In referring to the section titled "Oil-Filled Cables," it is noted that cable *T*-0.500 developed an increase in power factor due to the excessive sheath expansion drawing in core oil of high power factor. The cause of the high power factor of the core oil is not stated. If it is assumed that the oil was not abnormal, the operation could be considerably improved by carbon-black protection over the conductor. This would not only reduce the maximum stress, but would also improve the power factor of the core oil by adsorption. Similarly, if carbon-black tapes were used over the insulation, the severe discharges between the sheath and insulation would be eliminated, and the additional volume of carbon black would be helpful in conditioning the oil.

In conclusion 2 of this section, in the "Note," it is suggested that these investigations may be applicable to other oil-impregnated insulation, especially transformers. No doubt the transformer designers will discuss this point. However, in the event a cable session may escape their attention, I should like to point out that, to the best of my knowledge, the paper insulation used in transformers is quite different from the wood-pulp paper developed for cables, and hence the results obtained on the latter do not apply to the former.

In "Aging Tests of Three-Conductor Solid-Type Cable," the following statement is noted: "The special shielding tapes were either metallized-paper tapes or carbon-black tapes." Shielding, or stress control, is only one of several functions exclusively obtained in the carbon-black construction as ably demonstrated by Rosch[1] and further substantiated in subsequent investigations which will be published shortly.

In reference to "Mitigation of Sheath-Crack Troubles," it may be of interest to know that a terminal employing a flexible copper hose has been in successful service where formerly trouble had been experienced with lead sheaths due to fatigue from vibration. Similar construction could be employed in manholes though subject to some of the difficulties of the Sylphons pointed out by the author.

REFERENCE

1. THE TYPE *CB* IMPREGNATED-PAPER CABLE, S. J. Rosch. AIEE TRANSACTIONS, volume 59, 1940, pages 1041–9.

J. G. Stelzer (The Commonwealth and Southern Corporation, Jackson, Mich.): Mr. Halperin mentions that the movement of cable in ducts clogged with fine silt was much less than in ducts free of silt, this lesser movement being attributed to the increased friction caused by the silt. Isn't it possible to attribute, at least in part, this difference in movement to the fact that the heat radiation was much better in the ducts that were clogged with silt, thus reducing the over-all cable temperature with a corresponding reduction in expansion? It seems that this would be particularly true if the silt was moist.

Is joining any particular problem when cables are worked to the emergency war temperatures as given in Table VII? Are there any particular problems to be expected in connection with the migrations of the solid compounds?

In connection with the mitigation of sheath-crack troubles, we have had numerous installations where the cable movement at duct mouths was strictly limited by heavy concrete fireproofing. These cables all have a long record of continual operation with no reports of cracked sheaths or sheath abrasion troubles.

In localities where there are lead sheath corrosion troubles, will the increased temperatures under the emergency war loadings have any effect on the rate of corrosion? If so, in what way?

Table VII shows the maximum allowable conductor temperatures for normal and emergency operation. Conductor temperatures do not ordinarily mean much to operating men, and it is necessary to convert such ratings into terms of amperes for given periods of time. This conversion from temperature to amperes is a subject on which there does not seem to be much information available that does not involve calculations having factors that are not ordinarily available without considerable investigation. What method is used for converting these temperature ratings to amperes for the use of the operating personnel?

No mention is made of sheath swelling and bursting due to migration of impregnating oils. This is probably not much of a factor in the Chicago area where the contours are in general comparatively flat. Troubles of this nature probably are to be expected in hilly sections of the country if the emergency war loadings in Table VII are permitted.

Robert J. Wiseman (The Okonite-Callender Cable Company, Inc., Passaic, N. J.): Mr. Halperin's paper will be an aid to all who operate power cables in pointing out some of the important factors in installation and operation. I was very much impressed with the second paragraph under "Introduction." It summarizes very clearly the difficulty we have in trying to set up rules of procedure regarding power cables, and why it appears at times that the manufacturer will not give a clear-cut statement as to what a cable will do under certain conditions. I think everyone should read this paragraph and memorize it.

When comparing the results Mr. Halperin gets for the change in tearing strength of the paper after aging as a measure of deterioration with the results obtained at Massachusetts Institute of Technology in 1926–29, we should understand that the technique of cable manufacture has improved so that modern cables have less air and moisture, and better paper and oil, so that a longer life insulation should result.

Mr. Halperin refers to the breakdown of the outer tapes next to the sheath. As this is oil-filled cable, it seems as if the normal working stress would not cause failure, even though the oil had shown signs of deterioration, but the lightning and switching surges could start failure.

The views expressed regarding improvement in the thermal conditions surrounding a conduit system by replacing soil of high thermal resistance by soil with low thermal resistance or burying the conduit deeper are also applicable to buried cable systems. As Mr. Halperin states, going to greater depths results in a more constant ambient temperature independent of air temperature and a higher moisture content in the soil which is very helpful in lowering the thermal resistance and improving the heat dissipating properties of the duct or cable.

The emergency load temperatures proposed by Mr. Halperin seem reasonable for his company in view of his experience. As we can expect the loads on present-day cables to be raised because of war conditions, we can also expect the cable failure rate to increase. This will have to be accepted, but, fortunately, as Mr. Halperin brings out in his paper, some factors add and others subtract in their effect on operation, and so the net result seems to be that the cables will operate, even though we expect them to fail.

R. W. Atkinson (General Cable Corporation, Bayonne, N. J.): The subject of overload ratings of cables is being placed on a scientific basis by such contributions as Mr. Halperin's. In the absence of numerical data, the manufacturer was inclined to point out the dangers of any increase of rating above normal, sometimes indicating some of the directions from which such danger might come. On the other hand, the user who wished to be able to obtain an increased rating was inclined to discount the general warnings and to wish to base ratings only on proved limitations. The result was a wide difference in recommendations and no engineering basis by which these could be reconciled.

A further important difference has had to do with acceptance that exigencies or even economics of operation may justify deliberate shortening of cable life. The manufacturer was opposed to accepting a rating that might shorten life; the user was inclined not to recognize that increased ratings might affect the life unfavorably. The war emergency has made easier the resolution of this difficulty.

With this better viewpoint and with the data and analysis presented by Mr. Halperin, we have now reached a common ground where these ratings may be considered objectively and determined on a scientific basis with little difference of opinion between manufacturer and user.

William A. Del Mar (Phelps Dodge Copper Products Company, Yonkers, N. Y.): Mr. Halperin's paper stands out as a valuable contribution to the war effort, inasmuch as it points clearly and specifically to means of conserving copper and enabling the utilities to carry on, for the duration of the war, with existing equipment. He also suggests new and higher temperatures for normal operation.

The conclusions were reached as the result of studies on one of the largest cable systems in the country, where no effort has been spared to obtain the best possible operating conditions, even to the extent of enlarging a great number of existing manholes, and eliminating thermal bottlenecks.

Many cable systems do not enjoy these advantages, largely because of less favorable economic conditions; high temperature operation may, therefore, prove less successful. This is particularly the case where manholes are restricted in size, load factors are low, cables are old and deteriorated, and maintenance less than ideal.

It is, therefore, to be hoped that Mr. Halperin's proposals for normal operation will be accepted by other utility engineers as a challenge to bring out data either in substantiation or rebuttal.

Herman Halperin: As Mr. Shanklin correctly points out, in establishing increased load ratings, the gains due to greater utilization of cable circuits must be carefully weighed against the losses caused by reduction in life and by an increased rate of failures. In the present national emergency the problem is to obtain the maximum possible utilization in order to preserve critical materials without having these losses become very serious. As indicated in my discussion of the group of papers by Sporn, Gaty, Cole, and others at the AIEE winter convention in January 1942 (AIEE TRANSACTIONS, volume 61, page 432), it seems, however, shortsighted to increase loadings of cable, transformers, and so forth, so much that numerous troubles requiring extensive replacements of equipment will occur in a few years or, perhaps, before the end of the war. The emergency ratings are based upon the consideration that the cable would be subjected very rarely to such conditions and that the reduction in life caused by such occasional operation would, therefore, be comparatively small, while the advantage of the consequent general higher level of loading would be large.

Mr. Atkinson and others give emphasis to the advantage of obtaining and studying considerable field and test data in arriving at allowable temperatures and loading. As a result, the differences in opinions are decreasing. This sort of observation gives emphasis to the continuing need for convention sessions and discussions such as today's. These advantages become further obvious when it is realized that available information shows that European temperature limits for cable in 1939 were 0 to 20 or 25 degrees centigrade less than prescribed in regular American specifications and standards, and were still further below the limits given for emergency operation in my 1939 paper "Load Ratings of Cable."[1]

With regard to Mr. Shanklin's discussion of the advantage of deeper installation of conduit and cable, this method is used as an alternative to replacing poor soil, only if at the lower level better soil conditions prevail. Our test data on temperatures in and around conduits indicate definitely that the heat does not all come to the surface, but is conducted in all directions to the surrounding earth in accordance with the temperature distribution and thermal resistances. The thermal sink is not necessarily at the surface of the ground, especially in the summer. Also, with the lower conduits, the maximum base earth temperatures become four or six or eight degrees less in the summer, and this is usually of advantage because summer load ratings are becoming more and more the limiting ratings on cable. His suggestion to standardize on a definite depth for thermal rating is followed by the Commonwealth Edison Company in that all standard ratings are based on an assumed distance of four feet between the center of the conduit and the surface of the ground.

Concerning Mr. Shanklin's point on my proposed temperatures for normal daily loading usually being not greatly different from those in regular American specifications, it should be noted that the temperatures in those specifications were almost always used for special conditions. In other words, the usual daily temperatures were actually much below the temperatures given in the specifications. With the new temperatures given in my paper for emergency loading, a utility can, as a practical matter, load cables to have in normal daily operation temperatures such as in the specifications, or somewhat higher for wartime, as I indicated.

As to Mr. Shanklin's criticism of my 65-degree limit for solid-type 66-kv cable, I refer to the Halperin–Shanklin paper, "Studies of Stability of Cable Insulation,"[2] wherein it is shown that in laboratory tests at 2.5 times operating voltage, cables were stable with heating cycles to 65 degrees. These cycles were with a room ambient of 20 or 30 degrees centigrade, whereas in conduits the ambients would almost always be higher. Other Chicago tests showed stable cables with heating cycles to 70 degrees.

In connection with the point on the possible ill effects of high temperatures on accessories, it should be noted that these possibilities apply principally to extra-high-voltage accessories and that the accessories usually operate at temperatures substantially less than those for cable in conduit. The ill effects in Chicago were found in a laboratory where the ambient was the same for the accessories and cable.

Regarding Mr. Stelzer's point, I think that silt, provided it is wet, has some effect in reducing the copper temperatures, but the effect is almost certainly inadequate to account for the reduction found in cable expansion. Migration of asphaltic joint compound into cable insulation can cause high power factors adjacent to the joint, but we never experienced any trouble from this cause, because such compounds are used in joints operated at 12 kv and less, where high power factors are of minor importance, and in joints on old 22-kv lines consisting of belted cable having heavy insulation.

The method of converting allowable temperatures into allowable currents cannot be given here because of lack of space but has been described in a paper on "Operating Temperatures of Underground Cables" by C. A. Bauer (1930–31 report of engineering section, Great Lakes division, National Electric Light Association).

In answer to another of Mr. Stelzer's questions, increased temperatures will increase the rate of lead sheath corrosion where such action is already present to an appreciable extent. As to sheath swelling and bursting in hilly country, it is to be expected that impregnating compounds in solid-type cable will flow at usual operating temperatures, and the effect of higher temperatures is to increase the rate of flow. In Chicago we have many risers where the vertical elevation of cable is 20 or 30 feet.

Mr. Hickernell's point on conductor shielding for oil-filled cable seems to me to be particularly applicable for those three-conductor cables with sector conductors having maximum stresses over 200 volts per mil and for hollow-core single-conductor cable. I agree with him that conductor shielding has other benefits besides stress control. As to his point on the difference in papers used in cable and in transformers, some transformer manufacturers advertise their use of "kraft" and "cable" papers and I do not see any substantial reason to expect any large differences between performance of these various cellulose materials and of power cable paper subjected to heat.

In reply to Doctor Whitehead's question, the cables with thinner insulation showed more deterioration because all samples regardless of insulation thickness were tested at 24 kv, three-phase, with consequently higher stresses on the thinner insulation.

I agree with Doctor Wiseman that the insulation in modern cables should be expected to deteriorate less under heat than it did in the tests of 15 and 18 years ago at Massachusetts Institute of Technology.

Operating experience and studies thoroughly substantiate Mr. Del Mar's points that reasonable manhole sizes and cable training and good maintenance pay excellent returns in the way of longer life of underground circuits and good reliability of service.

The interest shown by all discussions is greatly appreciated.

REFERENCES

1. LOAD RATINGS OF CABLE, **Herman Halperin.** AIEE TRANSACTIONS, volume 58, 1939, October section, pages 535–56.

2. STUDIES OF STABILITY OF CABLE INSTRUCTION, **Herman Halperin, C. E. Betzer.** AIEE TRANSACTIONS, volume 55, 1936, October section, pages 1074–82.

Frequency Control of Load Swings

Discussion and authors' closure of paper 42-113 by J. E. McCormack and R. J. Lombard, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, September section, pages 623–4.

J. E. Hancock and **H. L. Clark** (General Electric Company, Schenectady, N. Y.): This system of load control really gives to the manually controlled generators a portion of the duty of the frequency-controlling unit since the manual load changes are to be made partially dependent on system frequency. It would appear that in addition to improving load swings on the tie feeders this load-dispatching method should also improve the system frequency regulation.

The two methods described for obtaining the necessary accurate frequency indications show considerable advantage over the conventional frequency meters. The usual frequency meter has also proved inadequate in investigations of steam turbine governor performance. To facilitate these investigations the General Electric Company engineers have recently developed a frequency meter operating on a entirely new principle. This new frequency meter is applicable in the load control method described by Mr. McCormack and Mr. Lombard.

The frequency meter is contained in a metal case approximately 5 by 7 by 10 inches and indicates the frequency of the 110- to 120-volt 60-cycle power system to which it is connected. The total power consumed is approximately five watts. Figure 1 and Figure 2 show the frequency meter which has two scales, one marked from 59.875 to 60.125 cycles per second, and the other from 59.75 to 60.25 cycles per second. Selection between the two scales is made by means of a toggle switch on the instrument panel.

This frequency meter uses a mechanical resonant system consisting of a small 60-cycle tuning fork instead of the usual reactors and capacitors. Since it is well known that the resonance of a mechanical system is, in general, sharper than that of an electrical system, it is readily seen that a frequency meter utilizing this principle may be made extremely sensitive. In fact the range indicated is the broadest that is feasible while a scale of plus and minus 0.01 cycle per second can readily be obtained.

Any mechanical system combining mass and a spring has at least one natural resonant frequency. If a disturbing force is applied to this sytem, the resulting vibration will depend on the relation between the applied frequency and the resonant frequency of the mechanical system. The relative phase angle between the disturbing force and the resulting vibration will shift through nearly 180 degrees in the narrow frequency band corresponding to the resonant peak.

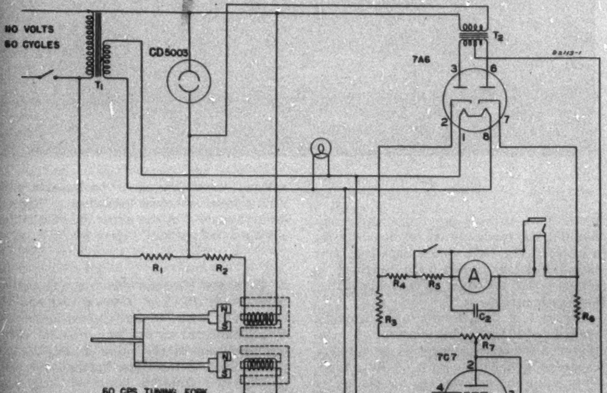


Figure 1

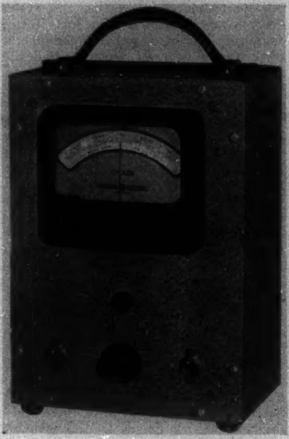


Figure 2. Supersensitive frequency meter—oblique front view

This frequency meter then consists of the tuning fork driven from the system frequency, and a vacuum-tube circuit measuring the phase angle between the system frequency and the resulting motion of the tuning fork. The accuracy of the tuning fork is so good that the over-all instrument accuracy depends only on the accuracy of reading the indicating instrument.

The marked scale of this frequency meter corresponds closely with the normal frequency band of a generating system, and each scale division is 0.005 cycle per second. Therefore, it will be seen that by estimating quarters of divisions, this frequency meter could be read to one part in 50,000. Thus, we believe that this development has brought the indicating frequency meter into a category which is comparable with master frequency methods.

S. B. Griscom (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors have made an important contribution to the operating technique of interconnected systems. While some of the principles brought out have been appreciated for some time and have been incorporated in automatic controls, these have not been well understood by the operating personnel, nor were they particularly adaptable to manual control.

In the operation of a large interconnected system, it will always be necessary to exercise a certain amount of manual control or direction, no matter how refined the controllers nor how widespread their use. Load curves have a definite pattern on a given system, but vary with the day of the week and seasonally. Availability of generating capacity varies with maintenance schedules, rainfall, and unscheduled outages, with individual station and system economy also playing a major role. Fitting these requirements together is the function of the system operator, according to flexible rules based on economy, reliability, and interchange contracts, all occasionally modified by contingencies.

The authors have shown that the best job of forecasting the system operator could do was not good enough if the orders were executed "woodenly." In other words, if a load drop was expected in a given area at a given time, this reduction should be ordered by the system operator, but not executed, unless it will tend to restore frequency to normal. With this procedure deviations from expected load changes will have a minimum disturbing influence on frequency and tie-line loadings.

Most of the control necessary for maintaining frequency and tie-line loadings can be done automatically, usually with advantage with respect to closeness of control, and with respect to freeing the operator from an onerous task. In addition to frequency and tie-line controllers, there is also available the "program load controller"[1] whereby the fluctuation in station output, instead of being taken by one machine, is distributed among all operating machines in the station, achieving the best possible thermal economy. This scheme has great practical value, as it relieves both system and station operators of continuous consideration of incremental thermal economy. The value of automatic control for any of these functions is, of course, dependent upon the rapidity with which load changes take place.

The stroboscopic means of showing each station operator visually the direction and amount of deviation from standard speed has much to commend it. In addition to permitting an improvement in manual control of load, it gives the operator a far better mental picture of cause and effect when manipulating the generator speed changers.

REFERENCE

1. TIE-LINE CONTROL OF INTERCONNECTED NETWORKS, T. E. Purcell, C. A. Powel. AIEE TRANSACTIONS, volume 51, 1932, pages 40–7.

J. E. McCormack and **R. J. Lombard:** The prime requisite for frequency control of load adjustments at generating stations is that the same indication of system frequency be presented to the operator at each station. This can be accomplished either by providing a standard frequency common to all stations and a suitable device for comparing with system frequency, or by individual frequency meters which have been compared for identity of indication and which will sustain their accuracy over a long period, regardless of atmospheric or other influences.

The instrument described by Hancock and Clark could be applied to frequency control of load swings, provided that it has the aforementioned characteristics and is suitable for use in generating stations. It appears to be a laboratory instrument with possibilities for conversion to a suitable operating meter if equipped with a readily visible scale and pointer, or possibly it could be adapted to provide the comparison standard for stroboscopic indication of frequency.

Mr. Griscom has taken this occasion to stress some of the problems encountered by load dispatchers and generating-station operators. Because of the innumerable situations that may occur in a station or upon the interconnections, automatic control must be supervised by the station operator and on occasion superseded by manual control. Automatic controllers relieve the operator of the labor of control, but the responsibility for correct operation remains with him. In view of this responsibility, he should have a thorough knowledge of system characteristics and be provided with sufficient information and adequate instruments for intelligent performance of his duties.

Series Capacitors for Transmission Circuits

Discussion and authors' closure of paper 42-112 by E. C. Starr and R. D. Evans, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, pages 963–73.

J. W. Butler (General Electric Company, Schenectady, N. Y.): I have reviewed and studied this paper very carefully. My interest was greatly stimulated when reading the synopsis, wherein it was indicated there had been some new developments in protective equipments for series capacitors. My interest continued throughout the body of the paper, but I confess I was unable to discern just what new developments were being described. Therefore, I will outline my reasoning in order that my quandary might be appreciated—and I hope clarified.

Previous series-capacitor equipments in power lines utilized protective equipment fulfilling the requirements set down by the authors. The first series capacitor ever installed in a power line—described in reference 6 of the paper published in 1928—used such equipment.

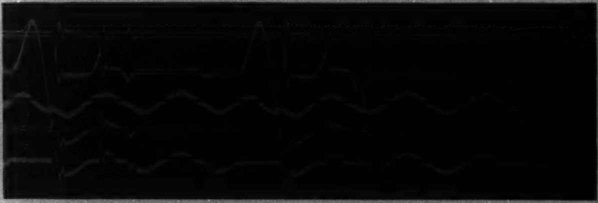


Figure 1

Figure 1 of this discussion is an oscillogram dated 1935 showing the speed of operation of a protective equipment for a series-capacitor installation on a 22-kv circuit. This setup was adjusted so the closing of the contactor reduced the current sufficiently to cause it to open, which of course resulted in an increase in current making it close again, so it "pumped," closing and opening. It is seen that the contactor makes a complete round trip in $2^{1}/_{2}$ to 3 cycles, which is within the time intervals specified by the authors. For applications on long lines where some short-circuit currents can be relatively small, it may not be desirable to effect immediate reinsertion of the capacitor—as suggested by the authors—because of unnecessary operation of the protective equipment.

Therefore, I would like to ask: "What are the new developments that prompted the writing of this paper?" And in spite of the statement in reference 1 of the paper, I would like it explained why "previous equipments decreased rather than increased" transient stability.

The conclusion in respect to the necessity of suitable damper windings on machines to be operated with series-capacitor lines checks with our previous analyses and experiences. The following quotation is taken from June 28, 1941 *Electrial World*, in an article "Amortisseur Windings for Hydro Generators" by Crary and Dungan. "If series capacitors are used in the line connecting the machine with the system, the tendency for the generators to hunt may be aggravated to such an extent that amortisseur windings would be required."

The authors tested two motor-generator sets to determine the effects of damper windings. The basic criterion of the effects of a damper winding on machine performance is the effect the dampers have on the machine subtransient reactances and time constants. I wonder if the authors could give us these values for the two machines in question.

The ratio of the inertias of these units as given is about 4/1, the copper damper machine being the larger. Thus, I would expect its natural frequency to be about one-half the no-damper machine, but the measured frequencies show the copper damper to have approximately twice the frequency of the other.

Our previous studies agree with the authors' conclusion as to the location and arrangement of series capacitors. Figure 3 in reference 7 of the paper is remarkably similar to the authors' Figure 5.

S. B. Crary (General Electric Company, Schenectady, N. Y.): Over a year ago at the 1941 AIEE winter convention, Mr. Evans indicated he was in agreement with our conclusion of the value of series compensation over the other methods of improving the stability limits of long a-c lines. We are particularly glad to have this conclusion further substantiated in the paper by Starr and Evans. Such confirmation is important since it allows the emphasis to be placed on the details of how series compensation may be best accomplished. In this respect, the paper by Starr and Evans is a valuable contribution, because it deals with those detailed questions which must necessarily enter into any practical application of series capacitors.

We have made studies of series capacitors somewhat similar to those reported in this paper. In 1928 a rather complete experimental test setup was made to check the stability of the system with and without series compensation for two and three synchronous machines. These tests indicated the safe limits of compensation and the degree to which the stability limits could be increased. Further analytical work has given additional quantitative information as to the limits of compensation. According to this work, the experimental results reported in the Starr and Evans paper indicate but one of the previously well-recognized boundaries of possible instability, that is, the hunting produced by large ratios of resistance to reactance in the transmission tie. As pointed out in a discussion by Concordia[1] there also exists a limit for the minimum amount of resistance that may be in the circuit. For practical systems this limit may be of greater importance than the one shown in the paper.

Our studies have also shown the advantages of placing the capacitors at the intermediate switching stations in the center section of the line where they will not be switched out with the line section.[2] Such an arrangement is desirable in order to reduce as much as possible the duty on the capacitor protective equipment.

Under "Types of Capacitors," the authors mention two kinds, the nonlimited- and the limited-voltage type, and indicate the desirability of using the limited type in order to reduce the cost of the capacitor. This is in general correct. However, it may be found desirable to prevent operation of the protective equipment for currents exceeding normal values and at least equal to those obtained during the largest angular swing at full-load which may be obtained without loss of synchronism. If the capacitors are capable of remaining effective in the circuit during such large angular swings, the over-all stability of the system will be improved for severe faults.

Under "Location in the Circuit" the authors point out there is little advantage in using more series capacitors than the number of intermediate switching stations. This statement is substantially correct when the switching stations are equally spaced along the line including the center portion of the line. The amount of compensation necessary is, of course, a matter of economics and performance characteristics. For a 234-mile line $66^{2}/_{3}$ per cent compensation reduces the equivalent line length from a reactance standpoint to 78 miles. Accordingly, such a degree of compensation for this length of line may not always be necessary but may be required for longer lines. The per cent compensation will tend to increase with the line length as well as with the amount of power it is desired to transmit. We would like to ask at what rate the required amount of compensation would decrease if the assumed transient stability limit margin were decreased. Since the economic study depends upon the assumed stability margin, this becomes an important factor for the application of series capacitors for improving the stability limits. It would appear that a 20 per cent transient-stability margin for the Grand Coulee system were unusually large if this were for the more severe type of transmission-line faults. Ordinarily a much smaller margin for the more severe faults may be used in the analysis, since the probability of obtaining these severe faults is itself small. Possibly the authors would explain in more detail why they elected to choose what seems to be a conservative transient-stability margin on which to base their economic study.

It has now become generally recognized that series compensation is entirely a practical and economic way to increase the power limits of long-distance transmission lines. The chief differences of opinion will probably arise from the assumed conditions of operation upon which their use is justified.

REFERENCES

1. Discussion by **C. Concordia** of STABLIITY LIMITATIONS OF LONG-DISTANCE A-C POWER-TRANSMISSION SYSTEMS, AIEE TRANSACTIONS, volume 60, 1941, page 1299.

2. THE EFFECT OF SERIES CAPACITORS UPON STEADY-STATE STABILITY OF POWER SYSTEMS, **E. J. Allen, J. L. Cantwell.** *General Electric Review*, May 1930, page 281, Figure 3.

A. J. Krupy (Commonwealth Edison Company, Chicago, Ill.): There is one point which has not been mentioned by the authors in connection with the economic justification of the series capacitor as compared with the three-circuit alternative for the 230-kv transmission described in the paper. The cost figures as shown in the paper assume an equal degree of reliability for both schemes of power transmission either for normal or abnormal conditions. In my opinion such an assumption is not justified.

I would be interested to know whether the authors are in position to make a statement as to the relative degree of reliability of the schemes considered? It would also appear that under the existing conditions, the vulnerability of the single concentrated capacitance center both to an accidental electrical failure or an intentional mechanical damage, is much greater as compared with that of the conventional transmission circuit for the third 230-kv line.

Aside from the obvious difference in the inherent reliability of the two schemes, in the case of a complete outage of the newly added equipment or structure, the switching out of one half of the circuit shown in Figure 5c of the paper still leaves a superior system from the stability viewpoint as compared with as shown in Figure 5a, should there be an electrical failure or a mechanical destruction of the series capacitor.

A. C. Monteith (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The use of series capacitors to compensate for the reactance of transmission circuits has been recognized as a technical possibility for about 50 years. However, before such applications of capacitors could be put on a practical basis, a number of technical developments were necessary, including:

1. Reduction in cost and increase in reliability of capacitors.

2. Development of high-speed circuit breakers and relays for transmission circuits.

3. Development of protective equipment that permits the use of capacitors with voltage ratings determined by load currents.

The authors' investigation has established the practical aspects of the use of series capacitors so that the electrical industry should feel more confident in making applications.

We have heard much about the desirability of doing everything possible to conserve vital materials. Here is a scheme that accomplishes this desire at no sacrifice in system operating reliability.

E. C. Starr and **R. D. Evans:** In preparing this paper, the authors had in mind three principal elements:

1. Reactance compensation is economically sound when applied to appropriate major circuits. Voltage transformation results in line-resistance as well as line-reactance conversion. The most economic combination of impedance conversion and reactance compensation is desired.

2. The usefulness of series capacitors on transmission circuits is to improve transient stability. Application requirements of capacitors and protective equipment have been determined for a particular case, and the necessary functional arrangement for a protective scheme has been provided.

3. Systems with normal degrees of reactance compensation will not have impractical operating characteristics.

These views have been well supported by the discussion on the paper.

Mr. Crary's comment concerning our paper as a contribution, because it deals with the questions which must enter into any practical application of series capacitors, takes on added significance when it is realized that

1. Previous work was concerned principally with steady-state conditions, whereas the controlling stability condition occurs from transient disturbances.

2. Our paper not only presents the first theoretical analysis and miniature-system test results of series capacitors under transient conditions but also describes the combination of equipment necessary to make the scheme practical.

The reference quoted by Mr. Crary relative to the minimum as well as maximum amount of resistance that may exist in the circuit for stable operation indicates that for the typical case of a 230-mile line with 66.7 per cent reactance compensation, which was analyzed in the paper, the minimum stable length would be of the order of 75 miles and the maximum several times the assumed length. Consequently, the two limiting resistance conditions, as pointed out in this reference, are remote from the example given.

Mr. Crary has raised a question as to the selection of a particular margin of stability as the basis for the economic study. The choice for a selected stability condition must, of course, be based on experience in connection with the particular system and the judgment of the operating engineer. There are obvious advantages in having a margin above the calculated limit, since this makes operation less dependent upon fault-clearing speeds and upon the actual distribution of load and generating capacity. In answer to this question we have, however, made a comparison between the addition of a series capacitor and the addition of a third transmission line for the extreme increase in load which would eliminate all margin in transient stability for the same terminal impedances. Under this condition the first cost of adding the series capacitor would be about 40 per cent of the first cost of a third transmission line. If capitalized figures for additional loss and reactive kilovolt-ampere generation are added to the first cost of the capacitor, the series capacitor can still be installed for 75 per cent of the cost of adding the third line. It is to be emphasized that the economic study was made on a very conservative basis in regard to cost of construction and in regard to load factor which controls the capitalized figures for line loss. For this reason when the actual stability margin, line-construction cost, and capitalized losses are taken into account, the results, we believe, will be more favorable than shown in the paper. If the kilovolt-amperes of an individual generator is less than the rating of a line, added series capacitors can be proportioned to the particular case whereas added lines will come in relatively large steps.

Mr. Crary concludes his discussion with the statement that it is now generally recognized that series capacitors provide an entirely practical and economical way of increasing the power limits of long transmission circuits. This is a definite change in the point of view concerning the usefulness of series capacitors previously expressed in the AIEE TRANSACTIONS, volume 56, 1937, February Section, page 264, the pertinent excerpt from which is as follows:

> "... To protect the capacitor against puncture the usual practice (in using series capacitors) is to use a normal voltage capacitor with means to short-circuit it during excess current conditions. The series capacitor as proposed is thus rendered ineffective at the time when it is most needed. The alternative is to use capacitors capable of withstanding the high voltages arising under fault conditions. In no major project in America has the series capacitor received serious consideration."

The foregoing was prepared by the AIEE subcommittee on interconnection and stability factors. Both Mr. Crary and Mr. Evans were members of this subcommittee and concurred in the views expressed. The change in viewpoint is believed to have resulted from the work reported in the present paper on economic gains and on transient stability with series capacitors. Previous articles, including items 7 and 8 of our list of references, have been limited to voltage-regulation studies and to steady-state stability conditions.

Mr. Butler's question of how series capacitors can reduce transient-stability limits and his request for more information on the protective scheme indicates a lack of understanding of the essential features of the series-capacitor scheme described in the paper. It is not sufficient merely to provide a series capacitor and means for short-circuiting it and subsequently restoring it to the circuit in response to the indications of a current-sensitive device. If a "protected" series capacitor is short-circuited upon the application of a fault, and if the fault is not quickly cleared and the series capacitor is not quickly restored to the circuit, the transient-stability limits will be reduced below the values which would obtain on the system without the capacitor in the circuit.

This condition results from the facts that

1. The excitation on machines will be slightly lower.
2. The initial angle between sending- and receiving-end machines will be slightly less.

Both of these conditions tend to reduce the transient-stability limit of the system with the series capacitors in comparison with the system without series capacitors. This viewpoint was back of the statement on series capacitors which was made by the AIEE committee on system stability and given in its report, an excerpt of which was previously quoted.

Table I. Subtransient Reactances of Miniature-System Machines

100-Kva Machine Damper	Reactance—Per Cent x_d''	Reactance—Per Cent x_q''
None	26.0	57.7
Connected copper	15.7	14.6

The essential features of a series-capacitor scheme for increasing transient stability have adequately been described in the paper. This scheme is based on using series capacitors of the protected type in combination with high-speed line-sectionalizing circuit breakers and relays. In this scheme the series capacitors are (1) short-circuited upon the occurrence of a fault which would produce excessive voltage and (2) quickly restored to the circuit after the fault is cleared, the combination functioning in a sufficiently short time to make a substantial improvement in transient stability over the value obtainable without the series capacitor. While the essential elements of the scheme are simple and easily understood, it involves a conception that is expected to make feasible the application of series capacitors for transmission circuits. Quite separate from this functional scheme are the details of the protective equipment. Space limitations made it desirable to omit the details of the protective scheme from this paper.

In general we are in agreement with the technical observations of Crary and Butler. However, the inaccuracy of Mr. Butler's characterization of the series-capacitor installation at Ballston in 1928 makes it necessary for us to point out that it was not an anticipation of the scheme described in our paper, because it does not meet the essential requirements of a scheme to increase transient-stability limits. Despite the complete mastery of the problem prior to 1935, certain misapplications of series capacitors subsequent to this date have placed the device in unfavorable light among some engineers.

We do not understand that Mr. Butler contends that either the protective equipment for Ballston or for the 22-kv installation which he mentioned is suitable for use with a 50-kv 100,000-kva capacitor installation on a 230-kv circuit, such as considered in our paper. Such a view of these protective schemes is consistent with the committee members' subsequent agreements to the statements on the limitations of protected capacitors from the standpoint of transient stability as given in the AIEE subcommittee report previously referred to. Such a view would also explain why no definite proposals of series capacitors for major transmission circuits have been made previously.

Mr. Butler has asked for additional information on the subtransient reactances of the miniature-system machines. The values are shown in Table I.

In reply to Mr. Krupy, we agree that there is probably more concentrated vulnerable equipment in the compensated two-circuit system than in the uncompensated three-circuit system. However, capacitors as now manufactured and provided with means for isolating a damaged unit should be essentially as reliable as switching or other terminal equipment and when properly installed should constitute no greater hazard to continuity of service than the more common equipment. They should be no more vulnerable to intentional damage than are generators, transformers, and circuit breakers.

The third circuit will under some abnormal conditions provide greater reliability, particularly if the third line is located on a separate right of way at increased cost. However, we should like to point out that the compensated two-circuit line can lose one section and remain stable under essentially the same loading as that for which the three-circuit line can lose one section and retain synchronism. If the two-circuit line loses both sections between the same switching stations, service will, of course, be interrupted. But if the three-circuit line similarly loses two sections between the same switching stations, the power capacity of the remaining circuit would be so low that if the line were loaded materially above 60 per cent of its rating, it would also lose synchronism and interrupt service. Consequently, as we view the comparison, the three-circuit uncompensated line has some advantage over the compensated two-circuit line under abnormal conditions, but, starting from normal circuit arrangements, their performance will be comparable for all of the more frequently occurring types of outages.

Mr. Monteith has outlined the steps in the technical developments necessary to put series capacitors for transmission lines on a practical basis. Consideration should be given, as he has suggested, to the conservation of vital materials made possible by this type of series-capacitor application. For the case considered in the paper the material required for the capacitor installation would be only a small percentage of that required for the third circuit.

Stability Study of A-C Power-Transmission Systems

Discussion and author's closure of paper 42-117 by John G. Holm, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, pages 893–905.

J. A. M. Lyon (Ebasco Services Inc. New York, N. Y.): The author of this paper has made two useful contributions on the subject of stability:

1. Stability studies have been made on a variety of hypothetical systems including some with lines of great length and with operating voltages which in some cases are beyond those now in use.
2. The many well-known contributing factors to system stability have been listed and studied.

According to the author, equation 2 of the paper enables the transient stability of a system to be found by comparison with a similar system of known transient-stability limit merely on the basis of the ratios of the reactances of the systems, the voltages, and the ratings of the systems. This relationship seems consistent with the factors affecting transient stability. However, it appears that the equation should be recognized as a means of obtaining an approximate result rather than an exact one. The ratio of system capacities does not seem capable of exact evaluation. The author's definition of system rating is too simple and requires further explanation. In the same way the author's claim that the expression is valid for systems of "similar configuration" is somewhat indefinite and should be explained further.

Figures 3 and 4 of the paper seem useful, but it is desirable to know if the author has checked either of these sets of curves by the known stability limits of actual systems which fall in the rather broad range of the systems of his computations. The curves of Figure 3 are called "estimating curves," and the range of usefulness should be proved as well as clearly defined.

Part II of this paper has indicated possible corrective measures to be applied to a system to increase the stability limits. The listing of these methods, together with the statement of the results of using some of these methods according to calculating board studies, seems valuable. Obviously the problem of improving the stability of a system involves many operating and economic considerations which the author has not discussed.

Upon reading the text on the use of reactors to increase stability limits it is not at all clear whether or not Figure 9 represents the increase of the stability limit due to the reactors alone or due to the reactors together with the larger generators required with reactors.

Many of the above comments have been in the nature of specific questions; the answers to these may very well increase the usefulness of the paper. The author should be commended for making available information on a large number of theoretical systems, as well as reviewing and checking many fundamental considerations of the stability problem.

S. B. Crary (General Electric Company, Schenectady, N. Y.): It will be realized by anyone familiar with stability calculations that the author of this paper has done a tremendous amount of work. He has made a worthy contribution by presenting the results of his study which include many of the factors entering into the transmission stability problem. This paper is concerned primarily with three methods of improving stability: intermediate synchronous condensers, generator bus reactors, and resistance grounding of the sending-end transformers.

All of these methods, as indicated by the author's references, have been considered, although no comprehensive study of their characteristics has been previously published. However, they have been carefully studied for particular systems. The conclusions, in general, which we have arrived at from these system analyses are:

1. The intermediate condenser for improving the stability limits of straightaway transmission lines is not so effective nor practical as the series capacitor which, in effect, reduces the equivalent line length.

2. Generator bus reactors have been found in general to be more expensive and less desirable as a means of improving the stability limits than the use of higher short-circuit ratio generators. Their use appears to be limited to stations which after completion have been found to have too low stability limits.

3. The use of resistance in the neutral of the sending-end transformers does provide a means for improving the transient-stability limits. The benefit from its use decreases with shorter switching time, higher generator inertia and is, of course, of no value for three-phase faults which are the most severe type. Accordingly, the use of resistance in the neutral may or may not be found necessary.

We would like to ask the author how he arrived at such a high value of equivalent reactance for the synchronous condensers (128 per cent). We would expect this reactance to be considerably less than this, particularly if the synchronous condensers are operating near full leading kilovolt-amperes.

F. Von Voigtlander (The Commonwealth and Southern Corporation, Jackson, Mich.): The author presents an unusually complete and readable paper on the subject of power-system stability. The premises on which the studies of the 31 assumed systems are based are of practical significance, and therefore the results are understandable and of value to those who must cope with stability problems in the industry. The reduction of the data to a series of tables and curves which can be interpolated and extrapolated for intermediate values is noteworthy and considerably extends the usefulness of the results.

Present conditions have greatly aggravated the problems of stability on many power systems because of the deferment of important system reinforcements and plant additions, upon which has been superimposed greatly increased war loads. These situations are a challenge to management to make the fullest use of the engineering ingenuity at its disposal so that the best possible service to essential loads may be provided by skillful disposition of existing facilities.

It therefore seems worth while to call attention to the fact that, in order to make full use of the potential stability limits of a power system, there are a number of factors to consider in addition to the power-angle relationships. Of these, the adequacy of protective relay and machine control systems is perhaps the most important.

High-speed fault clearance is most effective in mitigating stability troubles. Conversely, slow fault clearance can bring with it a host of complications. Unfortunately, slow fault clearance is quite prevalent, and often little can be done about it under present conditions, especially with older types of switchgear.

Practically all types of transmission-line relays may be subject to false operation during system oscillations, even within the theoretical stability limits of the system. The application of such devices should therefore be particularly well considered under these conditions. Auxiliary control devices may be required to assist in correct discrimination, for example, between an actual fault in a line section or the presence of an electrical center of oscillation there. In extreme cases, such correct discrimination may be impossible, even with the best equipment available, but often considerable improvement may be effected by comparatively simple rearrangements and minor additions.

Adequate control features for synchronous are also of importance, especially on condensers and on generators which may be motoring as condensers prior to the fault. Adequate excitation control and governor oil capacity may easily be the difference between success and failure of the system to ride through the oscillations incident to line faults, and again simple rearrangements, often within the scope of present restricted construction, can effect great improvements.

While the considerations of electrical and mechanical characteristics of machines, loads, lines, and switchgear are of fundamental importance in stability studies of electric-power systems, the application of controls to such equipment must be made with great care and with special emphasis on their behavior under emergencies so that they will permit the theoretical ability of the system to be actually attained. The practical power limits of systems may often be extended with only existing facilities by engineering ingenuity. Under the stress of the times, such efforts should easily be justified.

John G. Holm: The discussions presented do not lend themselves to a common reply; they will therefore be handled separately.

Mr. Crary prefers a series capacitor to an intermediate synchronous condenser for improving the stability limits of trunk transmission lines. It is true that the capacitor reduces the equivalent line length, but in the long lines the resistance is very large, and, if the line is to be economical, it will presumably transmit a large block of power, so that its current will also be high, even if the highest voltages are employed. Consequently the resistance drop is large even though it is assumed that lower-resistance conductors are justified economically. The longer the line, the larger the resistance drop which must be compensated by inductance, after allowing for some resistance to be left between the ends of the system. Therefore in the longer lines there will be a limit to the series-capacitor compensation that may be used, and consequently a limit to the increase in the stability limits obtained from this source.

It is embarrassing to disagree with a man of Mr. Crary's knowledge, but it seems to me that the series-capacitor compensation has definite advantages over the intermediate condenser only in lines from 150 to about 375 miles long, and no longer, assuming that the analysis of systems is made with actual loads and not on the basis of an infinite bus. However, so far as the economics of the question are concerned, it remains yet to be proved that even in 150- to 375-mile lines the advantage lies on the side of the series capacitor. So far, no economic comparison has been made between a line using series-capacitor compensation and the same line whose stability limits are raised to the same levels by the use of other means than the series capacitor.

As Mr. Crary points out, shunt reactors on the generator bus are more expensive than the higher short-circuit ratio of generators. So far as I know, the highest short-circuit ratio of generators ever used is somewhere around 3.0, and it is very unlikely that the increase in stability limits obtained from this short-circuit ratio approaches that obtained from shunt reactors. Moreover, the short-circuit ratio has a far greater effect on the steady-state-stability limit than on the transient limit, and is to be resorted to primarily where the steady-state stability limit is to be raised first. The shunt reactors therefore may be used in addition to the increased short-circuit ratio in cases where the latter fails to bring the transient-stability limit to the desired level. The cost per unit of improving the transient stability by the use of the shunt reactor will therefore not be so expensive as might appear at first.

Besides the particular applications of the shunt reactor outlined in the paper, the shunt reactor, as well as the resistance in the sending-transformer neutral, is particularly well adapted for application to existing systems with prevailing slow fault-clearing times when it is desired to raise their stability limits. The resistance may, of course, be used if the transformer insulation is sufficient.

Mr. Crary asks why the synchronous reactance of synchronous condensers was taken at 128 per cent. In the first place, I wish to make clear that while the saturated synchronous reactance of the receiving-end condensers was taken at 128 per cent, that of the intermediate condensers was taken at 72 per cent (0.8 of the unsaturated value for both the receiving-end and the intermediate condensers). I regret that this figure was omitted from the second abridged draft of the paper. The unsaturated direct-axis synchronous reactance of synchronous condensers is given at 160 per cent in the First Report of Power System Stability and in other sources.[1–3] At any rate, it seems that the value of the synchronous reactance of the receiving-end condensers has a very slight effect, if any, on the system stability limits.

Mr. Lyon has asked a number of questions in his discussion. I regret that the limitations on the length of the paper did not permit going into the various details, and I am glad that the questions have been brought up. They will now be answered.

Equation 2 of the paper gives results within five per cent of those obtained by actual measurement on the network analyzer. By systems of "similar configuration," to which the equation applies, are meant trunk transmission lines. The number of circuits, the sectionalizing of the lines, or the lines, or the units of the end equipment are of no importance. As for the definition of the "system rating," I may say that much thought has been given to it. Whatever definition is given has only a theoretical significance, because for the two systems in question the quantity P enters the equation as a ratio, so that care should be taken only that exactly the same definition is applied to both systems. If the two transmission lines are designed on the same principles, so far as the conductor current-carrying capacity, corona power loss, and the voltage regulation are concerned; if the power factors and line and transformer efficiencies are properly taken care of; and if the transformer capacity is installed to fit the above conditions, in a similar manner in the two systems; then the installed transformer capacities may be taken for the quantity P. If the two lines are designed on entirely different principles, then either design correction factors must be applied, or what is known as the reliable rating of the line when there are no faults on it must be used. Care should be taken that criteria applied to both systems are as nearly as possible alike.

Mr. Lyon asks if curves of Figures 3 and 4 of the paper have been checked with stability limits of existing systems. There are very few systems whose stability limits have been reliably reported. The curves, however, have been checked on two systems, and a check was obtained well within engineering accuracy. The difficulties of such a check, however, should not be underestimated in view of the approximations necessitated by the network analyzer and the difficulties of evaluating and representing the system load. The stability limits determined on an actual system will never completely agree with those obtained through measurements on the network analyzer. The curves of Figures 3 and 4 are based on the study of a certain range of systems. They can be used for any fairly economical combinations of kilowatts, mileages, and voltages. When a case is taken of transmitting a certain block of power at a voltage entirely out of proportion to the transmission distance, a point on Figure 3 will be obtained which might lie either between the separate curves or entirely outside them.

Figure 9 represents the increase in the stability limits due to reactors with a corresponding increase in the kilovolt-ampere generator capacity, as discussed in the paper. I regret that this point was not made clearer. However, the increases in the stability limits due to reactors alone may be calculated by one of the methods outlined in the paper.

Mr. Lyon regrets that the paper does not discuss the economic considerations of the improvement in stability limits. There are so many factors entering into system costs that perhaps it would be desirable to present in a separate paper the costs and the effect on them of the various methods of stability improvement.

Mr. Von Voigtlander justly points out the importance of high-speed relaying and fault clearing of the transmission line. So far as system stability is concerned, there is nothing so effective as high-speed fault clearing. Mr. Von Voigtlander's statement that the system control equipment should be adequate for responding properly during emergencies as well as when the system is near its stability limits could not be made any stronger.

In conclusion I wish to thank the discussers kindly for the trouble they went to in preparing their discussions and for the contributions they made.

REFERENCES

1. FIRST REPORT OF POWER-SYSTEM STABILITY, AIEE subcommittee on interconnection and stability factors. AIEE TRANSACTIONS, volume 56, 1937, February section, pages 261–82.

2. THE REACTANCE OF SYNCHRONOUS MACHINES, R. H. Park, B. L. Robertson. AIEE TRANSACTIONS, volume 47, 1928, pages 514–35.

3. SYMMETRICAL COMPONENTS, C. F. Wagner, R. D. Evans. McGraw-Hill Book Company, Inc., New York, N. Y.

Analysis of Short Circuits for Distribution Systems

Discussion and author's closure of paper 42-88 by Charles F. Dalziel, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, October section, pages 757–64.

F. S. Benson (Pacific Gas and Electric Company, San Francisco, Calif.): This paper is timely as a source of additional information for computing short circuits on low-voltage distribution systems when delta-wye or delta-delta transformer banks are installed, serving both three-phase and three-wire single-phase loads. The equations and formula developed in the paper will be helpful in determining the magnitude of short circuits on low-voltage or secondary systems.

I agree with the author that resistance plays an important part in limiting low-voltage line-to-ground short-circuit currents; all resistances should be included in the computation, even though they are of such low magnitude as bolted connections between wire and bar, or wire and wire. Field experience has shown that the magnitude of low-voltage line-to-ground short circuits is usually considerably less than that computed by ordinary methods; using reactance values only, we have found, if resistance is included, the computed values of short-circuit currents (line-to-ground) are in a reasonable agreement with those occurring in practice.

The graph in Figure 2 shows that the effect of ground resistance in limiting short-circuit currents is greatest at low voltage, that is, 800 volts or less, and is least when the transformer-bank secondary voltage is on the order of 12 kv. If, however, internal faults occur within such transformer banks, the effect of the ground resistance is quite important in reducing the magnitude of the line-to-ground voltage, even though the transformer secondary voltage may be as high as 12 kv. This is probably true because high-voltage systems are usually solidly grounded at the source of power and a transformer-bank internal fault adds an extensive zero-phase sequence network in series with low-voltage ground resistance.

The analysis of the magnitude of transformer reactance during transformer internal faults is interesting; the author's tests tend to confirm his conclusions that the transformer reactance can be entirely neglected when computing the value of such faults. The elimination of the transformer reactance from computations during transformer internal faults will aid in determining the correct and economical size of case ground wire or secondary-neutral ground wire to the grounding device.

Figure 16 is instructive and again substantiates my experience that ground resistances on the order of one ohm or less do not necessarily limit the rise of voltage on the transformer secondary neutral to the prevailing idea of a low value, when transformer internal faults occur. When distribution transformer banks are installed near substations having large power supply, and the ground resistance is a large proportion of the total impedance to the fault, the voltage on the distribution transformer secondary neutral may approach the line-to-ground value of the primary voltage supplying the bank, although the ground resistance is considerably less than one ohm.

Charles F. Dalziel: The author is pleased to note that experience on actual power systems confirms the conclusions derived in the paper. This is significant, since all of the experimental data were obtained from tests made on small laboratory transformers operated at reduced voltage. The interpretation to the effect that, during transformer internal faults, the transformer may be neglected entirely, should be qualified slightly. As stated in the paper, it is believed that this simplification should give satisfactory results for cases in which the feeder impedance is at least equal to 50 per cent of the transformer impedance in high-voltage ohms. The change from solid to dash lines in Figure 16 indicates the proposed limit. The approximation may result in excessive error for transformers located near the bus. Obviously, neglecting the effects of the transformer would result in very serious error for transformers directly connected to a bus of maintained voltage.

Attention should be called to the fact that practical units are specified throughout the paper. Although per units may be used directly in most equations, corrections involving $\sqrt{3}$ are necessary when using per unit quantities in analyses involving unsymmetrical conditions in wye-delta transformations. This is true for the power-leg and light-leg equations derived in the paper, and practical units were specified to avoid introducing unnecessary complications.

Steady-State Theory of the Amplidyne Generator

Discussion and author's closure of paper 42-89 by Troy D. Graybeal, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, October section, pages 750–6.

J. L. Bower and B. H. Caldwell (General Electric Company, Schenectady, N. Y.): The author has developed an interesting and useful method of predicting the characteristics of an Amplidyne generator with the aid of constants which can be measured from open- and short-circuit tests. We have been using a similar method, which is based on tests made on a large number of machines for a wide range of applications and have found in the general case that certain modifications of the theory presented by the author are required.

The author has confused speed-voltage coefficients with inductances. In general the author's terms L_{AA}, M_{A-s}, and so forth, which are speed-voltage coefficients, are not equal to the corresponding inductances, even though the units are the same. Also the mutual inductance between two windings is the same when viewed from either winding, while the mutual speed-voltage coefficient between two windings usually has different values when viewed from the different windings.

The steady-state equations 6 and 7 do not include terms which represent the speed voltage developed in the main- or direct-axis armature winding due to direct-axis armature current and the speed voltage developed in the cross- or quadrature-axis armature winding due to quadrature-axis armature current. These coefficients are primarily functions of the centers of current collection of the direct- and quadrature-axis currents. The effective center of current collection is a function of the brush position and all of the factors affecting commutation, such as magnitude of current, commutating field adjustment and brush characteristics (both mechanical and electrical) which vary with brush material, speed, commutator film condition, and so forth. Unsymmetrical spacing of the windings also gives rise to some degree of mutual coupling between all of the various windings, but experience has shown that for the normal case only the ones discussed above have sufficient effect to warrant their addition to the author's equations 6 and 7.

Adding the term $SM_{BB}i_B$ (the speed voltage developed in the quadrature-axis armature winding by current flowing in the same winding) and the term $SM_{AA}i_A$ (the speed voltage developed in the direct-axis armature winding by current flowing in the same winding), we obtain the steady-state equations

$$-i_A(R_A+SM_{AA})+i_BS(L_{AA}-M_{A-qB})=V_A$$

$$-i_AS(L_{AA}-M_{A-dA})-i_B(R_B+SM_{BB})+i_sSM_{A-s}=0$$

The open-circuit equations become

$$i_s=e_s/R_s$$

$$i_B=\frac{SM_{A-s}}{R_A+SM_{BB}}i_s$$

$$V_A=\frac{S^2(L_{AA}-M_{A-qB})M_{A-s}}{R_B+SM_{BB}}i_s$$

and the short-circuit equations are

$$i_s=e_s/R_s$$

$$i_A=\frac{S^2M_{A-s}(L_{AA}-M_{A-qB})i_s}{(R_A+SM_{AA}(R_B+SM_{BB})+S^2(L_{AA}-M_{A-dA})(L_{AA}-M_{A-qB})}$$

$$i_B=\frac{SM_{A-s}(R_A+SM_{AA})i_s}{(R_A+SM_{AA})(R_B+SM_{BB})+S^2(L_{AA}-M_{A-dA})(L_{AA}-M_{A-qB})}$$

and the load equations are

$$i_B=\frac{-S(L_{AA}-M_{A-dA})i_A}{R_B+SM_{BB}}+\frac{SM_{A-s}i_s}{R_B+SM_{BB}}$$

$$V_A=-\frac{(R_A+SM_{AA})(R_B+SM_{BB})+S^2(L_{AA}-M_{A-qB})(L_{AA}-M_{A-dA})i_A}{R_B+SM_{BB}}+\frac{S^2M_{A-s}(L_{AA}-M_{A-qB})i_s}{R_B+SM_{BB}}$$

The terms SM_{BB} and SM_{AA} appear in the same manner as the resistances R_A and R_B. These terms SM_{BB} and SM_{AA} can be and frequently are considerably larger than R_A and R_B. It is immediately apparent that the term SM_{BB} plays a very important rôle in determining the load characteristics; and since the coefficient M_{BB} is a function of load and speed, this variation instead of saturation and nonlinearity of brush drop, may well account for a considerable part of the curvature of the curves of Figure 8. Also the difference in characteristics with the different grades of brushes shown in Figures 6 and 7 might well be due to differences in commutating characteristics of the brushes rather than to difference in brush drop.

The author uses a negative sign in equation 5 before the speed-voltage coefficient, $SM_{A-qB}i_B$, which is the speed voltage developed in the direct-axis armature winding by current flowing in the stator coil in the quadrature axis. This is the convenient convention for the author's case, since the coil qB is assumed connected so as to partially compensate for armature reaction in the quadrature axis. Although the validity of the analysis is unaffected, it is of interest to note that in the usual case, particularly for small size machines, this winding is connected so as to increase the flux of armature reaction.

The author gives an appropriate summary of the difficulties involved in calculating the characteristics of the Amplidyne generator without the benefit of the constants which he determines experimentally. The designer must have the solution of this more difficult problem in order to build a machine which will have characteristics required by a specific application. However, as is often the case, the simplified method outlined by the author is a definite contribution toward the solution of the more difficult problem, since its use aids in the development and intelligent application of experience factors which simplify and improve the accuracy of the designer's calculations.

P. Lebenbaum, Jr. (General Electric Company, West Lynn, Mass.): The author is to be complimented on his analysis of the Amplidyne generator and the excellent check he has obtained between theory and test.

As has been pointed out, the Amplidyne generator is a dynamoelectric power amplifier. The three fundamental requisites of a good amplifier are its amplification ratio, its fidelity, and its speed of response. Commercial Amplidyne generators have calculated amplification ratios of 30,000 to 150,000 to 1, and time constants of less than one eighth of a second between signal current and output voltage. The time constant of the machine, while slow compared to the vacuum-tube amplifier, is nevertheless extremely small when compared with those of other machines in the circuits where the Amplidyne generator is most frequently applied.

The importance of good commutation and correct brush position in both short-circuit and load axes cannot be overstressed, for the brush short-circuit currents and magnetizing or demagnetizing ampere turns because of brush shift affect all three amplifier properties. The author's analysis assumes that the brushes are on the inductive neutrals in both axes and that the brush short-circuit currents are zero. If these assumptions were not true, equations 4 and 5 would contain terms involving mutual inductances between the direct and cross axes, and succeeding equations would be changed. However, if the Amplidyne generator has been properly adjusted for commutation with all brushes on their respective neutrals, these additional terms are not needed in the steady-state analysis.

An example of the importance of brush short-circuit currents is the author's test shown in Figures 6 and 7. The difference in saturation curves of the experimental Amplidyne generator because of change in cross-axis brush material is due as much to change in cross-axis brush short-circuit currents as to change in brush drop. The experimental machine was apparently compensated properly with the soft graphite brushes in place, for the curves of Figure 7 agree closely with theory, the open-circuit voltage varying approximately as the square of the speed for constant signal current. However, the change to a harder brush, while keeping the same compensation in the commutating field, results in undercompensation of the coils undergoing commutation. It can be shown that undercompensation in the cross axis results in a directly demagnetizing effect in the signal field axis. The higher the speed, the higher are these demagnetizing ampere turns. Thus the open-circuit voltage no longer varies as the square of the speed, and hence the "crowding" and nonlinearity of the curves of Figure 6 results.

Troy D. Graybeal: The discussions by P. Lebenbaum, Jr., J. L. Bower, and B. H. Caldwell are both pertinent and interesting. The representative data for commercial Amplidyne generators given by Mr. Lebenbaum are particularly significant.

Concerning the addition to the equations of the two speed terms which represent the voltages generated in either axis of the machine by current in the same axis, these terms theoretically should be included if a more accurate solution of the steady-state operation of the machine is desired. However, the apparent increase in accuracy cannot be realized in actual practice, because the coefficients M_{BB} and M_{AA} are critically dependent upon the condition of the brush contact surfaces, as well as upon the brush position. For example, the slight "play" between ordinary brushes and brush holders will cause a large increase in these constants when the direction of rotation is reversed. A slight shift of the brush position from the true neutral, or a slight change in the surface conditions, because of the minute arcs between brushes and commutator caused by normal load current, might make the coefficients several times larger. Figure 1 shows the values of ohmic resistance and of SM_{BB} plotted against speed. The two nearly linear curves show that while M_{BB} was constant for each test, a change in cross-axis current changed the value of the "constant" by a factor of four. This increase was due to the change in the distribution of current over the brush surface, even though no change had been made in brush position or commutating pole adjustment.

For the calculation of the curves given in the paper, an average of the cross-axis circuit equivalent resistance was taken as shown by the dotted curve of Figure 12 of the paper. The actual ohmic resistance was 1.16 ohms, which was increased to 1.55 ohms to give the equivalent resistance used for the calculations. This procedure is justified by Figures 10 and 11 of the paper which show that the curves calculated on this basis do give a very close approximation to actual test results.

The theory presented in the paper was developed by calculating the speed voltages which are generated in the armature of the machine. The coefficients which appear in the final expressions are ratios of flux linkages to currents and therefore are coefficients of inductance. These coefficients would be identical with the open-circuit inductances if perfect commutation could be obtained in the exact geometric neutral. The fact that these conditions cannot be fulfilled in an actual machine gives rise to effects which reduce the output of the machine. Actually, these effects represent a reduction in the numerical values of the coefficients of inductance compared with those obtained from measurement with the coils open-circuited and the brushes of the machine raised. The fact that the performance of the Amplidyne generator cannot be calculated on the basis of inductances obtained from the usual methods of calculation or from usual inductance test procedure does not mean that the coefficients which appear in the equations are not inductances, but rather that the inductances are different under actual conditions of operation. These differences arise because the distribution of flux is not the same as is usually assumed for calculation, or as that which exists during open-circuit inductance measurements. Since Bower and Caldwell prefer to call the coefficients which apply in actual operation "speed-voltage coefficients" rather than inductances, no doubt they find that this terminology avoids confusion in their work with Amplidyne generators.

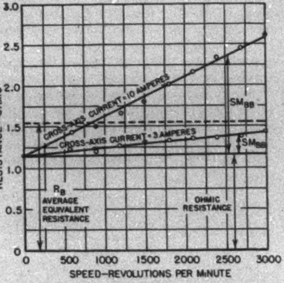


Figure 1. Curves showing the components of the equivalent cross-axis circuit resistance of the Amplidyne generator and the variation of these components with speed

The negative sign mentioned was used in equation 5 because the flux of the commutating pole in the cross axis always opposes the flux of armature reaction. If in addition an additive coil in the cross axis is used in an attempt to increase the cross-axis flux, only a slight increase can be obtained. The additive turns increase the resistance of the cross-axis circuit and therefore decrease the current. This smaller current flowing through the increased number of magnetizing turns produces only a slight increase in flux. The additive coil will increase slightly the machine efficiency and also tend to mask out the nonlinear effects introduced by the brushes, the commutating voltages, and so forth, in the cross-axis circuit. On the other hand, omission of this coil will improve the speed of response of the Amplidyne generator to changes in the signal current.

The purpose of this paper was to present the theory of the Amplidyne generator in a form suitable for use in applying the generator to control mechanisms. For this purpose, simple expressions of the operation are essential, because the Amplidyne generator must be studied, not as an isolated unit, but in relation to all other units of the control mechanism. Information on details of design can be obtained from tests of experimental machines; these "experience factors" are helpful to the designers but are seldom published, and therefore are not available to the purchaser. It is hoped that this paper will encourage the publication of the results of other investigations which no doubt have been made upon the Amplidyne generator.

Precision Speed Control for World's Largest Induction Motor

Discussion of paper 42-132 by R. R. Longwell and M. E. Reagan, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, September section, pages 634–8.

E. Herzog (U. S. Army Air Corps, Wright Field, Dayton, Ohio): A few explanatory notes may be in order in regard to the authors' presentation of this particular control problem.

Essentially two controls, the start–stop switch and the speed setting, permit the operator to control the entire assembly in normal operation. The one brings the fan motor to standstill, ready to run, or shuts it down from that point; the other controls it through the entire speed range from standstill to maximum speed. As long as the equipment performs properly the operator merely supervises the correct functioning of the control. The total investment in the installation is, however, so great, and the need for its use so urgent at certain times that the drive could not be held inoperative because of a failure of the automatic control. For this reason control can be transferred from automatic to manual at any point in the operating cycle. To facilitate repairs after completion of the run, the control can be rapidly subdivided and reassembled in various groupings.

With modern tendencies toward automatic control there may be no need to explain the particular set-up, but the authors pointed out the elaborateness of the control, and, as has been stated in this discussion, the control is in some respects only semiautomatic.

The large investment in the equipment is obvious. The new type of equipment precluded the possibility of using operating personnel familiar with its operation, and the enormous expansion of the plant made it most improbable that trained operators could be obtained in the present state of the labor market. The expectations were actually fulfilled; trained personnel have to be scheduled carefully for key positions. The automatic control described has made it possible to operate equipment of considerable complexity with a minimum of trained personnel leading a large group of semitrained employees. The government engineers specified the control requirements, and the co-operation of the electrical industry has been most gratifying in this and many other cases when it has been necessary to fulfill unusual requirements.

Electrical Features of Design and Operation of the Plantation Pipe Line

Discussion and author's closure of paper 42-143 by M. A. Hyde and H. B. Britton, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, September section, pages 638–44.

W. E. Stueve (nonmember; Oklahoma Gas and Electric Company, Oklahoma City, Okla.): Since I have been interested in the electrification of crude oil and oil-products pipe-line pumping stations for a great number of years and have conducted negotiations for my company resulting in the complete electrification of certain transcontinental crude-oil pipe lines, I was given an opportunity to discuss this paper by the authors.

I think the engineers are deserving of the highest praise in working out the design of the motors used in the pump stations, which design enables the operators to increase the motor output 50 per cent by merely increasing the ventilation or air-cooling arrangement of the motor. Thus, on the 12-inch section of the line 600-horsepower motors are used for original capacity, and the same motors will deliver 900 horsepower later on, after the intermediate booster stations are installed. This problem always arises in most crude-oil pipe lines being installed to handle the flush production of newly discovered oil fields.

Another contribution, which the design electrical engineers have made to the pipeline industry, is the recognition they give to high load factors or capacity factors at which most pipe-line pumping stations operate. The motor is designed for low starting torque since the centrifugal pumps are always started against a closed header gate valve with consequent low rotor resistance, resulting in very good efficiency of the motor itself.

Another achievement accomplished by the design engineers is the elimination of a fire wall between the pump and motor. Most early-day pipe-line operators were reluctant to install electric motors for pump station use on account of the apparent fire hazard. The elimination of the fire wall reduces the investment in the pump station which tends to make the use of purchased power more economic.

The automatic control features embodied in the design of the pump stations are also an achievement of note, since they eliminate the human element to a great degree in the operation of the pump station. Certain labor savings are thereby accomplished which again tend to make the use of electric power more economic.

The only criticism I would care to offer is one of a mechanical nature perhaps but which has to do with the over-all flexibility in the pumping operation. In my experience in negotiating contracts for pump-station electrification, I was always confronted with the statement of manufacturers of other forms of prime movers that, since the electric motor speed could not be efficiently changed, and the induction motor was essentially a constant-speed prime mover, it did not offer the proper flexibility, when changes in capacity were desired. This operating flexibility was discussed very briefly by the authors, but in my judgment this should have been enlarged upon to a greater degree by showing what actual capacities could be achieved at high efficiency points of both motor and pump in a long pipe line similar to the Plantation Pipe Line containing a great number of pump stations. Thus, as an example, if we assume a level pipe line employing four pump stations with two pump units in each station arranged in series, the following capacity rates can be realized:

> Eight pump units or all motors on this hypothetical line, of course, will provide 100 per cent capacity; six pump units, properly selected, will provide 86 per cent capacity; and five pump units, properly selected, will provide 78 per cent capacity. One pump unit in each station will provide 70 per cent capacity. Intermediate values of capacity between those selected above can be achieved by throttling the discharge to a certain extent without lowering the pump or motor efficiency very much.

As stated previously, the dual horsepower design of a motor, permitting the increased loading of the motor or decreased loading of the motor, as the case may be, is an outstanding achievement, for in the case of crude-oil pipe lines the loads are always greater at the outset, because of the flush oil production being handled with a resulting decrease in capacity later on. In the case of gasoline or oil-products pipe line the reverse is true, in that the original capacities are low until the business is built up, and as more capacity is required the larger loading of the motor becomes necessary. All of this eliminates tearing out motors and purchasing new equipment as time goes along, and this design accomplishes results by merely adding or taking off a fan on the rotor to increase or decrease the ventilation.

M. A. Hyde: It is of interest to note that the provisions made in the original design of the line for capacity increase already are justified. Since the paper was written, it has been found desirable to increase the capacity of the main line, and fourteen new intermediate stations are now under way, which will provide 50 per cent more throughput. When this work is completed, the aggregate motor horsepower on the system will be 51,000 horsepower.

Analytical Treatment for Establishing Load-Cycle Ratings of Ignitrons

Discussion and authors' closure of paper 42-94 by D. E. Marshall and E. G. F. Arnott, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, August section, pages 545–8.

O. K. Marti (Allis-Chalmers Manufacturing Company, Milwaukee, Wis): In their paper, the authors D. E. Marshall and E. G. F. Arnott, have solved in a very clear and practical way the problem for rating of rectifier tubes subjected to different load cycles, but by doing so they have considered only their thermal characteristic and acceptable arc-back frequency as the limiting factor.

During the last two years, the Allis-Chalmers Company has been conducting field as well as laboratory investigations in order to determine the influence of the inverse current on arc-back frequency. It was found that the arc-backs are in a certain relation to the inverse current and that this current is an important factor for determining the rating of rectifier tubes. In most cases it is possible to cool the rectifier in such a way that no internal parts of the tank or tube reach a critical temperature. However, it is not so simple to arrange the baffling in order to limit to a certain degree the ionization during the inverse cycle or the inverse current without introducing disturbing arc phenomena. Therefore, besides the temperature of the cooling water, the deionizing properties of the tube also will have to be taken into account. This means that the inverse current, which is no doubt responsible for terminating the length of life of the tube, has to be considered when rating a rectifier.

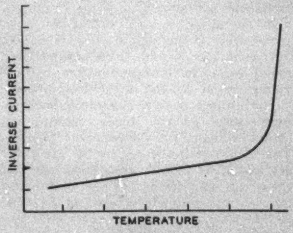


Figure 1

As can be seen in Figure 1 of this discussion, after a certain temperature the inverse current increases very rapidly and may reach a value which can endanger considerably the life of some internal parts of the tube. It is therefore necessary, when

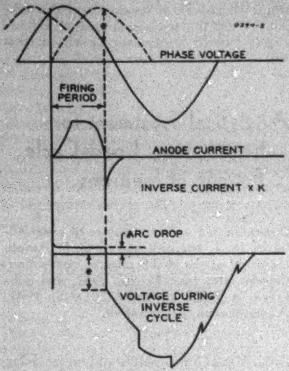


Figure 2

attempting to rate a tube, first to determine the temperature at which the inverse current does not exceed a certain safe value, and only then is it advisable to employ an analytical treatment such as outlined by the authors. The backfire test, as outlined by the authors, is no criterion for the rating because at higher d-c voltage the backfire frequency is not only a function of the cooling (or of the inverse current) but also of the anode voltage during the inverse cycle, especially of the value following the firing of the anode. (See *e* in Figure 2 of this discussion.) In other words, the inverse current of a given tube is a function of the cooling as well as of the voltage impressed between the anode and cathode during the inverse cycle.

In case a tube is being used in a welding circuit, or a six-phase rectifying circuit, it will have to be realized that for the same temperature of the tube a considerable difference in the backfire frequency may be found, due to the difference of the negative voltage (e) at the end of the firing period.

For instance, a curve illustrating backfire temperature against anode current, as shown in Figure 4 of the paper on "Sealed-Tube Ignitron Rectifiers" by M. M. Morack and H. C. Steiner holds good only for the circuit used during this test, and therefore, the rating cannot be based on the level of load which the tube will carry with acceptable backfire frequency.

In order to obtain very definite operating and rating characteristics for a given tube, it will therefore be necessary to take into account the above considerations regarding the current and voltage during the inverse cycle.

Reference

1. Sealed-Tube Ignitron Rectifiers, M. M. Morack, H. C. Steiner. AIEE Transactions, volume 61, 1942, August section, pages 594–9.

D. E. Marshall: Mr. Marti has offered several interesting and pertinent remarks concerning methods to use in determining the basic rating of a mercury-arc rectifier. We are, in general, in agreement that among other things the arc-back rate should be a function of the inverse current and the wave form and magnitude of the inverse voltage. In fact, our paper is actually based on such an assumption. We assume, that, for a given magnitude of inverse voltage at a given wave form, the other variable factors influencing arc-back are the demand current and the pressure of mercury vapor. The latter two determine the inverse current. Experiments performed in the Westinghouse Electric and Manufacturing Company on an unshielded ignitron a number of years ago indicate that for a given water temperature the inverse current is a nearly linear function of the demand current, other factors being constant.

In welding control circuits, the inverse voltage wave form depends on the line voltage, the power factor of the circuit, and the degree of phase control. Sealed-off ignitron tubes are rated for the worse combination of the above factors. This determines the voltage wave form. The current wave form is also set by the above setting of power factor and phase control of the test circuit. Thus the transition voltage is definitely set, and the rate of change of load current is directly proportional to the magnitude of the demand current. The water temperature is set at the maximum rated temperature.

Under the above conditions, the problem of rating the tube reduces to specifying the magnitude of the demand current allowable for a given duty cycle.

Our method assumes that the probability of arc-back at a given duty cycle will not exceed that of the basic test if the equivalent temperature of the vapor between anode and cathode does not exceed that of the basic test. This, we assume, will be the case if the heat energy stored in the tube structure and vapor never exceeds that stored in the basic test. We further assume that the stored energy is equal to the tube loss minus the heat carried away by the water and air-cooling facilities of the tube.

This would seem to agree with Mr. Marti's comments as follows:

1. The inverse current will be directly proportional to demand current.

2. If the arc-back rate is proportional to inverse current, then by item 1 it is proportional to the demand current.

3. Since, in general, the number of arc-backs that can be tolerated is measured in elapsed time and not in actual conduction time, then it would seem that the product of conduction time and demand current should be made constant. This means that the average current in the tube should be constant. This conflicts with the original assumption that the maximum equivalent vapor temperature be not exceeded. However, if the equivalent vapor temperature is kept constant, the average current criterion is within safe limits.

Mr. Marti's comments regarding rectifier operation as directed to the paper by Morack and Steiner are well taken in that the following factors need to be specified when the quality of performance of a given rectifier tube is judged.

1. Type test circuit.
2. Type load
    - Resistive
    - Inductive
    - Counter electromotive force
3. Inverse voltage wave form.
4. Rate of change of anode current at commutation.

The preceding four factors are interrelated and can be specified quite accurately by specifying the type circuit, the regulation, phase control, and loading.

Material for many papers could be found in the thorough investigation of the effect of circuit constants on the reliability of rectifier operation. It is hoped that these discussions will stimulate further research along this line.

Sealed-Tube Ignitron Rectifiers

Discussion and authors' closure of paper 42-106 by M. M. Morack and H. C. Steiner, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, August section, pages 594–9.

O. K. Marti (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): This paper outlines a very interesting account of the development which has taken place during the last few years in connection with sealed-off single-anode Ignitron type of rectifiers.

I would like to point out that a practically backfire-free operation and a low arc-drop single-anode rectifier can also be obtained when using a continuous ignition-excitation system, in spite of the fact that a cathode spot is maintained during the inverse cycle. In other words, it is not necessary to use an ignitor for initiating a cathode spot every cycle in each tank. Operating experiences with the Allis-Chalmers "Excitron" rectifier with a continuous ignition-excitation system have definitely proved that the small excitation arc does not increase deionization during the inverse cycle to such an extent that it contributes to the arc-back susceptibility.

I had occasion to call attention to this fact in my paper "Excitron" rectifiers, presented during the 1940 winter convention of the AIEE.

A cross section of a rectifier tank with continuous excitation, as introduced by the Allis-Chalmers Company over three years ago is shown in Figure 1A of the paper. This tank is somewhat larger than the one shown by the authors in their Figure 2 but is about equally shielded. A unit with six such tanks was in operation for several months on 600 volts and finally went into operation, supplying load to an industrial plant at 250 volts, 2,000 amperes, with occasional overloads of up to 3,000 amperes. This unit has been carrying such loads for over a year without a single backfire, in spite of the fact that the shielding is very slight, having a grid with relatively large holes. This particular "Excitron" rectifier tank had an arc drop comparable to that of the "Ignitron" rectifier, even at a lower current, that is, up to the current rating of the ignitor tube. This may show that the "Excitron"-type rectifier does not require more deionizing properties in spite of the fact that the excitation fires over the whole cycle. Naturally no difficulty is encountered in building a sealed-off rectifier tube using the continuous ignition-excitation system.

The use of two ignitors and a holding anode as shown in Figure 1 of the authors' paper complicates the design of the Ignitron rectifier and also requires a relatively complicated circuit with considerable auxiliary equipment. We would like to know if the authors consider such complication of the ignition-excitation system necessary only in connection with rectifiers used in the industrial and railway field, where the load fluctuates greatly and where periods of very low loads are encountered. Furthermore, does the introduction of a holding anode reduce the arc drop and the surges at low temperature?

Under the heading, "Ignitor-Excitation," the authors refer to a rather serious limitation, which is encountered during low load periods with the ignitor system. It may be of interest that the previously referred to "Excitron" unit was for several months operating on very low load, sometimes in parallel with a motor-generator set or convertor, but no difficulties were encountered in carrying loads of a few amperes.

REFERENCE

1. "EXCITRON" MERCURY-ARC RECTIFIERS, O. K. Marti. AIEE TRANSACTIONS, volume 59, 1940, pages 927–31.

J. H. Cox (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): I was happy to note the statement in this paper regarding the relationship between arc-drop and arc-back frequency in any given type of mercury-arc rectifier. This relationship exists in all types of rectifiers and should be recognized.

In Figure 3 of the paper it is noted that the arc-drop curve of the Ignitron described is plotted in terms of instantaneous anode current. In general, the user of rectifiers is interested in the arc drop in terms of cathode current. I would like to ask whether the authors find that the cathode current per group may be applied to this curve or if the average arc drop is influenced by the various rectifier transformer connections because of the difference in the length of the conducting period. In general, the arc-drop voltage is higher at the beginning of a conducting period, before maximum ionization density has built up, and this would suggest that those circuits utilizing the shorter conducting periods would have somewhat higher average arc drops My own experience indicates that the difference is negligible.

I was interested in the authors' statement that the Ignitron described does not exhibit surging characteristics down to a cooling water temperature of the order of ten degrees centigrade. In general, Ignitrons are much superior to multianode rectifiers from this point of view, because of the short arc length and small amount of shielding. In our experience with pumped-type Ignitrons we have never found any instability in the arc, with suddenly applied loads up to 300 per cent rating at water temperatures as low as six degrees centigrade, the minimum water temperatures available in Pittsburgh in the wintertime.

In Figure 5 the authors indicate double two-phase circuits for the utilization of four tubes. All multiple-group rectifiers require interphase transformers and enough inductance in the load circuit to maintain multiple-group operation. The advantage is a more effective use of the transformer windings and lower maximum currents in the anodes. In the case of six tubes the advantages far outweigh the disadvantages. However, in the case of double two-phase operation, the disadvantages become greater and the advantages less. A straight four-phase transformer is definitely no larger than a double two-phase with interphase. The lowering of anode duty is not so great as in the six-tube case. The operation of a double two-phase rectifier is definitely more critical, and the simplicity of the straight four-phase circuit has much in its favor.

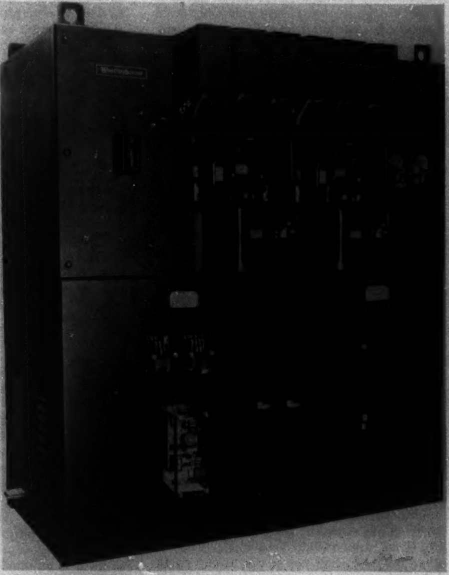


Figure 1. Sealed-off Ignitron assembly

The figure of 100 watts per ignitor for the Rectox-charged capacitor excitation circuit seems low, and I believe that 200 watts would be more nearly correct.

I do not agree with the statement that fuses for the interruption of arc-back currents are satisfactory. We have hardly arrived at the point where arc-backs are so rare that the inconvenience of fuse replacement for every arc-back would be negligible, and in view of the relationship between arc-drop and arc-back frequency, I do not think that such a low arc-back frequency would be desirable. I believe that the simplest and most attractive over-all arrangement for these small rectifiers is the use of disconnecting fuses in the primary circuit of the rectifier transformer and anode breakers in the secondary circuit of the transformer with only a disconnect in the cathode circuit. Such an arrangement provides the most complete protection, since modern air breakers are considerably faster than oil breakers, and by the use of anode breakers maximum protection is provided both for the rectifier and for the associated transformer.

By the use of anode breakers the complete switchgear, with the probable exception of the primary disconnecting fuses, can be mounted in a cubicle with the rectifier and the a-c switchgear unit shown by the authors in their Figure 10 can be eliminated. Figure 1 of this discussion shows a sealed-off Ignitron assembly in which the complete switchgear, with exception of the primary disconnecting fuses, is included.

The authors indicate, and suggest, the use of raw water directly in the Ignitron tubes. In many cases the cooling water available is such that trouble is encountered with scaling and deposits which interfere with water flow. Furthermore, best sealed-off Ignitron-rectifier operation is obtained with a high water rate, and such a rate is not possible with the circuits shown in the authors' Figure 13, unless temperature control is abandoned and water consumption prohibitive. Of course, by the addition of a pump, the raw water can be recirculated in a local path and still retain direct water cooling with high velocity and control.

In many of the authors' statements they give figures of efficiency and losses without mentioning the voltage of the application. I assume that in all these cases they are discussing 250-volt rectifiers.

M. M. Morack and H. C. Steiner: Mr. Marti's experience with the continuously excited single-anode mercury pool rectifier is very interesting. As Mr. Marti points out and as stated in the paper summary, it is the single-anode design with its low density of ionization during the inverse cycle which reduces the shielding necessary to prevent arc-back and thus lowers the arc losses.

In considering the method of cathode-spot excitation, the principle of igniting the spot each cycle offers two advantages:

1. With continuous excitation it is necessary to provide some form of cathode-spot stabilization or, as shown in the cross section of the Excitron tube, to insulate the pool in order to prevent the arc leaving its surface and anchoring on the tube walls.
2. It is felt that phase control of the output voltage is essential in the sealed type of rectifier.

The ignitor provides these functions with a minimum of complication in the design of the tube itself. Control of the ignition impulse serves the dual purpose of ignition and phase control.

The addition of a second ignitor is simply insurance. Actually, experience indicates that the ignitor is a very reliable device. Failure is more often due to abuse or misapplication, or lack of understanding its characteristics than anything else.

The holding anode was added as a result of field experience which indicated that minimum load conditions were likely to be encountered in sets of relatively high current capacity. Its addition did not change appreciably either the arc drop or surge conditions.

The arc-drop data in Figure 3 were determined in a three-phase single-way circuit using a vacuum-tube amplifier and cathode-ray oscillograph. The data are the average of the voltages at the beginning and end of the full-current conducting period. The arc drop is roughly a volt higher at the beginning and a volt lower at the end, when there is an excess of ionization. It would seem, as Mr. Cox suggests, that the arc drop should be higher in circuits with shorter conducting periods. However, our experience indicates that the difference is negligible.

In regard to the circuit, we believe that circuits of the multiple type with interphase are preferable because of the greater transformer utilization and lower tube arc losses. Our experience indicates that the double two-phase circuit is fully as stable as the double three-phase. The control is particularly stable since the average output voltage is the same at light load and 90 degrees phase control (four-phase operation) as at full load with 0 degrees phase control (two-phase operation).

The excitation loss in the capacitor circuit was determined by wattmeter measurement. The measured loss, including an assumed 50 watts in the Thyratron-tube heater circuit was 110 watts. Volt-ampere demand is several times this value.

Whether fuses are satisfactory for arc-back protection depends primarily on the frequency of arc-back. For industrial use we do not believe that frequent arc-backs (one or two per month) are permissible in sets of the capacity considered here. If the rate is one in several years, as field experience is beginning to indicate in certain installations, fuses become practicable. Practice seems to indicate that a-c feeder breakers are usually required for transformer and arc-back protection and that double pole d-c breakers are required to remove short circuits. Anode breakers to provide the arc-back and short-circuit protection functions have proved quite satisfactory in certain installations.

Whether it is preferable to design the rectifier and switch gear as one unit or to combine separate units with the advantage of highly specialized design and manufacture in each would seem to depend primarily on the manufacturing organization.

The efficiency and loss figures were all based on 250-volt rectifiers. This omission has been corrected in the final paper.

Energy Flow in Electric Systems—The *Vi* Energy-Flow Postulate

Discussion and author's closure of paper 42-141 by Joseph Slepian, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, December section, pages 835–41.

C. E. Bennett (Washington, D. C.): Ever since the time of Maxwell, students of electricity and magnetism have been intrigued by the problem of the mechanism of electric-power flow. Doctor Slepian's paper is a further contribution to the literature and presents interesting mathematical speculations without appearing to add anything tangible to our fund of knowledge regarding flow of electric energy or to impugn the validity of established engineering principles.

Doctor Slepian's paper is concerned, in general, with what he refers to as energy-flow "postulates." If the term applies to the mechanism of energy flow, one cannot quarrel with its usage. But if it refers to the flow itself, that is something that is not a "postulate." Whether it be a "phenomenon which can be actually physically observed" is a matter of indifference. Its effects, like the effects of heat, may be observed, and, as long as it may be measured accurately through its effects, the engineer will not worry about its observability. Whatever the nature of electric energy and the mechanism of its flow, the energy transfer itself is as much a reality as the metal conductor by which its flow from generator to load is implemented.

Throughout the paper, it is necessary to distinguish between energy flow as a reality and the mathematical assumptions and conclusions used in visualizing the mechanism of such flow. In his opening statement Doctor Slepian says:

> "Let V be the electric potential at any given point referred to some arbitrarily chosen point of zero potential."

Applying that definition to a three-phase circuit, if the neutral wire is assumed at zero potential, the result is the universally accepted one of energy flow in or about the three conductors. But if the point of zero potential be assumed on one of the three line conductors, then it appears from the application of these mathematical conventions that there is no energy flow in (or around, or adjacent to) that conductor (although it gets as hot as the others), and all of the energy flow is in or about the remaining conductors. Changing the hypothesis to assume another conductor at zero potential changes the apparent energy flow in that conductor to zero and seems to cause energy to flow in or about the conductor that previously carried nothing. Yet every engineer knows that grounding one conductor or another of an ungrounded three-phase system does not alter the energy flow. The writer of this discussion does not deny the occasional usefulness of such concepts, but he wishes to caution against confusing the consequences of mathematical assumptions with physical realities.

For practical reasons, engineers measure power flow by methods related to the totality VI product rather than by the summation of point functions discussed by Doctor Slepian. There does not appear to be anything in the paper to raise question as to the propriety of such methods, nor does the present commentator know of any meter or other device capable of integrating the point functions of Slepian and Poynting.

It should be noted that the voltages and currents associated with the conductors needed to interconnect generators and loads are determined by terminal conditions of the network and by the constants of the individual circuit elements. In consequence, the amounts of energy transmitted, lost, and stored or received, at any instant in association with any circuit element are fixed by the indicated parameters. Established engineering principles reveal the direction and magnitude of power flow through these elements but not the mechanism. The point-function "postulates" discussed by Doctor Slepian merely attempt to provide a mathematical picture of the mechanism of such flow. So far as concerns his use and control of electric energy, the engineer does not care whether the power flow takes place in the ether surrounding the conductors or in the conductor through the interatomic space by electron displacement, by a flow of electrons, by rotation of atomic dipoles, or by other means.

Against such a background some electrical engineers have read with surprise the eighth paragraph of the "Introduction" to Doctor Slepian's paper in which he says:

> ". . . . . the conditions just given are not sufficient for uniquely determining an energy flow . . . . which generation point is to feed which consumption point, and by what route, is completely undetermined so far as concerns any phenomenon which can be actually physically observed. Infinitely many postulates may be devised which will all be equally valid and equally well established by the conditions which have been described, and which are the only conditions which are available for defining an energy flow."

Doctor Slepian chooses not to elaborate on this point but allows it to appear as an incidental corollary, the relevancy of which is not disclosed in the paper. But Mr. Wagner, who presented Doctor Slepian's paper in the author's absence, and one of the engineers who took part in the discussion indicated that current interest in the mechanism of flow of electric energy has been aroused among engineers by its having been injected into certain regulatory Federal Power Commission cases,* in which decision turns upon the tracing of energy flows in a network. Doctor Slepian appeared as an expert witness in these cases and presented testimony for some 22 days regarding the flow of energy according to various energy-flow "postulates."

---

* It seems likely that the cases referred to (one of which is still pending before the commission) were Federal Power Commission dockets *IT*-5,563 and *IT*-5,565.

The full implications of the quoted statement become clearer from a consideration of Doctor Slepian's sworn testimony in the above cases. The following statements are noteworthy:

> "There is no location in the geographic sense to the energy *per se*. You may calculate a certain energy by means of certain measurements made of objects having certain locations in space, but that does not mean necessarily that the energy resides there." (Slepian, FPC docket *IT*–5,563, transcript page 3,011, line 10.)
>
> "The best location I can find for the energy, if I must have one, is in the mind of the calculator, strange as that may sound, if you must have a location." (Slepian, FPC docket *IT*–5,563, transcript page 3,012, line 10.)

The Federal Power Commission summarized Doctor Slepian's testimony regarding application of his views as to energy flow in a formal opinion (FPC docket *IT*-5,665, opinion 75, page 27), as follows:

> "Such a method, *it was admitted* [italics by the discusser], would permit allocating to a generator in Hartford, Conn., the supply of a load in Shanghai, China, and to the Shanghai generator the supply of the load in Hartford, if such equality were observed, notwithstanding the fact that the Hartford generator appears to be electrically connected to the load in Hartford and the Shanghai generator to the load in Shanghai, while there appears to be no electrical connection between Shanghai and Hartford."

This is interesting as mathematical speculation, but, if considered as applicable to the physical world in which power companies operate, a theory of energy flow leading to such results would raise question as to a utility's legal right to collect money for electric energy used by its customers.

A certain difficulty of understanding Doctor Slepian's paper is due to the notation and may be clarified by an examination of section V, "Example of Failure of Simple *VI* Postulate." Discussing the case of a varying current flowing in a conductor having inductance but no resistance, he says:

> "Hence if the simple *VI* postulate is valid, there should be appearing in the section an increasing amount of some form of energy, such as heat, for example. But no such energy appears there! The simple *VI* postulate fails!"

The explanation—his assumptions make it fail! By hypothesis, *V* is a scalar quantity determined by the location of point charges. It therefore excludes potential caused by induction. Doctor Slepian is simply pointing out that the *Vi* product excludes energy stored by induction. The "*V*" and "*i*" as used in his paper are not, therefore, to be confused with the corresponding Poynting vector quantities nor with the instantaneous values of potential difference and current which the engineer uses in the ordinary differential equations of electric circuits. Statements regarding the inexactness of the simple "*VI* postulate" should not, therefore, be interpreted as proving or disproving the exactness of the differential equations of electric circuits ordinarily used by electrical engineers.

S. W. Roland (Arlington, Va.) and **J. J. Jessel** (Falls Church, Va.): Doctor Slepian states that "the conditions which a valid postulated electric energy flow must satisfy . . . are insufficient for its unique determination. An analysis of this statement first requires that the term "postulated electric-energy flow" be defined.

The author obviously makes no distinction between measured and postulated electric-energy flows; that is, between energy flows that are continuously being measured and recorded at numerous points throughout an electric-power system and postulated flows which may be arbitrarily assumed for analytical purposes to exist at any point in space. He takes no cognizance of the fact that measured energy flows on the transmission and distribution circuits of a power system arise from consumer demands for electric power and that postulated flows, wholly independent of those demands, arise only when and if they are introduced by the analyst.

If one grants this distinction between measured and postulated electric-energy flows, it is evident that the question of uniquely determining energy flows on a power system concerns itself only with measured energy flows, particularly when such determinations are made in connection with legal proceedings. The term "energy flow" is a convenient expression used ordinarily to denote the transfer, transmission or conveyance of electric energy by electric currents.

The second question involved in the aforementioned statement by Doctor Slepian then becomes: "Can a unique determination be made of the measured electric-energy flows on a power system?" Before proceeding with an analysis of this question, it should be pointed out that it is common practice for electrical engineers to prepare diagrams showing both the magnitude and direction of power flows on the circuits of electric-power systems.

The 1942 edition of the "American Standard Definitions of Electrical Terms" includes the following definitions:

> *A-c transmission* is the transfer of electric energy by alternating current from its source to one or more main receiving stations for subsequent distribution.
>
> *Radial feeder* is a feeder supplying electric energy to a substation or feeding point which receives energy by no other means. (The normal flow of energy in such a feeder is in one direction only.)
>
> *Interconnection tie* is a feeder interconnecting two electric supply systems. (The normal flow of energy in such a feeder may be in either direction.)
>
> *Power* is the time rate of transferring or transforming energy.
>
> *Wattmeter* is an instrument for measuring electric power.

The above definitions indicate clearly that the Sectional Committee on Definitions of Electrical Terms under the sponsorship of the AIEE, "representing some 33 organizations, including national engineering, scientific and professional societies, trade associations, government departments, and miscellaneous groups," recognizes that

> 1. Electric energy is transferred by electric currents from its source to points of consumption.
> 2. At any instant electric energy is transferred in a single ascertainable direction.
> 3. The time rate of such transfers can be measured by means of wattmeters.

Whether or not the afore-mentioned definitions are considered by the "trained mathematician" to be naïve, they do, nevertheless, in the words of their authors, "express for each term the meaning which is generally associated with it in electrical-engineering work in this country." It cannot be truthfully said that the authors of these definitions are "lay engineers."

The ascertainment of the magnitude and direction of energy flow on a circuit of a power system by means of appropriate instruments thus constitutes the unique determination of energy flow for that particular circuit. In many cases this information is sufficient to enable the engineer to make the unique determination of the energy flows from the various generating stations to the various loads on the system. In other cases, however, a knowledge of the magnitude and direction of energy flow in each circuit of a network may not suffice to enable the engineer to establish uniquely a continuity of individual flows from one circuit to another from a particular source to some specified point on the system. To illustrate, assume that each of several transmission circuits delivers energy from a separate source to one end of a substation bus; also, that several feeder circuits connected at the other end of the bus transmit energy to a number of distribution points. From which particular source or sources is each distribution center supplied? This question is a particularly sterile one to the engineer; yet it has been propounded to a number of engineers in legal proceedings involving the transmission of electric energy in interstate commerce. It appears that a solution to this question appropriate to jurisdictional problems involving the transmission of electric energy in interstate commerce will ultimately be forthcoming from administrative bodies and from courts. Thenceforth the engineer can guide himself accordingly in treating with problems of this type.

W. A. Lewis (Cornell University, Ithaca, N. Y.): I agree with Doctor Slepian in his derivation and his conclusions, as presented in this paper. However, from the standpoint of a reader whose background, in comparison with Doctor Slepian's, is somewhat more that of an engineer, and considerably less that of a physicist, I would express his conclusions in a different form. Doctor Slepian states that the *VI* postulate, as defined in the paper, is not generally valid when applied to any arbitrarily chosen volume in space, unless the additional term derived by him is included. I would prefer to say that the *VI* postulate, without the correction term, is valid when applied to a properly chosen volume, the properly chosen volume being one for which, at every point of the bounding surface, either the E_2 component of the electric intensity, as defined by Doctor Slepian, or the magnetic intensity H, is zero or perpendicular to the surface. With this restriction, the contribution to the energy-flow normal to the surface provided by the correction term of Doctor Slepian becomes zero, so that the integral obtained by the uncorrected *VI* postulate gives the entire energy flow through the bounding surface and therefore is completely valid as applied to this surface. This is true for the following reasons. If E_2 or H is zero, the correction term becomes zero. If either E_2 or H is perpendicular to the bounding surface, the energy flow, proportional to the vector product of E_2 and H, must be perpendicular to both E_2 and H and will therefore be tangent to the plane of the bounding surface. If the energy flow at the surface is zero, or if the direction of the energy flow is tangent to the bounding surface, the integral over the surface becomes zero, and the total correction term is therefore zero.

If this concept is applied to the examples cited by Doctor Slepian, it will be found that the volumes selected in the examples conform to the restrictions just given, whenever the unmodified *VI* postulate is valid, and do not conform whenever Doctor Slepian finds it to be invalid. The electrical engineer, in treating energy flow, generally obtains correct results because the bounding surface conforms to the restrictions imposed even though the engineer may not have been entirely conscious of that fact in planning the connections of his meters.

With these facts in mind we may say that Doctor Slepian has shown how the *VI* postulate may be corrected whenever the volume over which we wish to apply it does not conform to the stated restrictions.

The idea that the bounding surface involved in a problem must be specially chosen is not confined to the uncorrected *VI* postulate. For example, the Maxwell scalar potential given by equation (1) of the paper is obtained by integrating over all spaces, as stated by Doctor Slepian. As a matter of fact, the integral may be correctly obtained if it is extended only over all charges which are sufficiently close to the point in question or sufficiently large to provide a contribution to the potential integral. Any surface so selected that it includes all pertinent charges will enclose a suitable specially selected volume over which the integration may be carried out.

One additional caution must be exercised in the practical application of the *VI* postulate, in the unmodified form. Since V (of the *VI*) is actually V_m the Maxwell scalar potential, referred to a single common reference, care must be exercised to insure that the potential used, and impressed on the wattmeter potential coils, actually corresponds to V_m. As V_m at any point is obtained by taking the line integral of E_1 from the arbitrary potential reference point to the point in question, care must be used to select the reference point so that E_2 makes no contribution to the potential, or that it makes the same contribution to the potential at every point of the bounding surface where the current density is not zero, so that its effect will cancel when the complete integral is formed. The last follows because the total current flow normal to the surface of any closed volume is zero.

If the bounding surface is so selected that E_2 is zero or perpendicular to the surface at every point, the point of reference potential may be any point in the surface, and the paths of integration from the reference point to points where the several conductors cross the bounding surface may follow any path lying in the surface. Further care must be exercised, however, if some portion of the bounding surface has been selected perpendicular to H instead of E_2, since E_2 may then have a component tangent to the surface which might then contribute to the V_m integral if the path of integration passes through such a portion of the surface.

Myron Zucker (The Detroit Edison Company, Detroit, Mich.): This paper is important in its implications as to power-system interconnections, including questions of where the power generated in one station is consumed either through conductive transfers or by wireless methods.

It is unfortunate that the conventional analysis of this problem is treated so condescendingly in the paper. While it is true that the "*Vi*" postulate is tenable only under certain boundary conditions, these conditions are perfectly reasonable and are understood by the engineer in solving his problems. They are the same type of restrictions that must be set up in any scientific analysis and are similar to some of the assumptions that the author has had tacitly to make in his more general deductions.

The impression of the argument is also not improved by the neglect of resistance loss in the second example, when it provided the entire "sink" for energy in the case of discharging the sphere. Everybody knows that resistance loss accounts for an appreciable portion of the energy in the case of flow along wires, and it would seem as easy—and much more convincing—to admit that this occurs but to point out that it could not begin to satisfy the energy input under the conditions laid down by the author. To the layman (speaking from the mathematician's heights) who would have occasion to follow the analysis, the case would be much stronger for this modification.

At any rate, the paper shows definitely that there is more to the story of energy flow than simply adding together a few wattmeter readings.

Joseph Slepian: I wish to thank the three discussers from Washington for their remarks, which so well bring out and emphasize the major points of my paper: namely, that there are infinitely many, equally valid, energy-flow postulates and that the particular one frequently used by power engineers gives correct results as usually applied, for usual engineering purposes on power systems, but needs a correcting term to have the universal validity of the other widely used postulate, namely, the Poynting vector, and the many other valid postulates which may be and have been devised.

As Mr. Bennett brings out, electrical quantities are observed, defined, and even measured by the effects which are associated with them. In the case of electric-energy flow, these effects are the appearance of electric energy at generating points and the disappearance of electric energy at consumption points. All energy-flow postulates which are valid associate properly with and may be said to produce these observed effects. In this sense, all the various valid energy flows are equally well observed.

In his third paragraph, Mr. Bennett presents the interesting family of valid energy-flow postulates given by $P_3=(V+a)I$, where a is an arbitrary constant. The convention as to the zero of potential most generally accepted by power engineers is to take the earth as having zero potential. With this convention, the *VI* postulate makes the energy flow in any conductor proportional to its voltage relative to earth. If one conductor of a normally ungrounded three-phase system is grounded, the energy flow in that conductor according to the *VI* postulate is zero, and the reading of a wattmeter with potential coil connected to that line and ground is also zero. With a balanced load, the wattmeters in the other two lines show different readings, and one may even read in the opposite direction to the other. Nevertheless, the engineer will add up these readings, knowing that they will serve properly for the usual proper purposes for which valid energy-flow postulates are applied by power engineers.

Now Mr. Bennett points out that we may equally well take the neutral point of the system as the zero of potential. This will shift all potentials of the system by a constant a. All wattmeters will now give different readings and indicate different postulated energy flows in the individual lines. Nevertheless, as Mr. Bennett says, these different energy flows lead to the same results for usual proper purposes. This is because $P_3=(V+a)I$ is a valid energy-flow postulate. As Mr. Bennett says so well in his fourth paragraph, the engineer is indifferent as to the detailed individual energy flows asserted by the particular postulate he uses, so long as it meets his needs in "his use and control of electric energy."

I am very glad that Mr. Bennett referred to the Federal Power Commission hearings at which I served as an expert witness. The transcripts of these hearings make very interesting reading and may be obtained from Elecreporter, Inc., Washington, D. C. My own testimony in *IT*-5,563 runs from page 1,458 to 1,669 and from page 2,264 to 3,890. In *IT*-5,565 my testimony runs from page 2,102 to 2,242.

At the hearing (in *IT*-5,563), serving as expert witnesses and expressing opinions essentially similar to my own, in so far as they were permitted by the extraordinary rulings of the examiner, were R. S. Shankland, head of the physics department at the Case School of Applied Science; W. A. Lewis, head of the electrical engineering department at Cornell University; R. W. Sorensen, head of the electrical engineering department at California Institute of Technology, and past president of this Institute; V. Karapetoff, emeritus professor of electrical engineering at Cornell University. Also ready to testify, but not permitted by ruling of the examiner, was J. Stratton, professor of physics at Massachusetts Institute of Technology.

These hearings were carried on professedly following the procedure of a law court. The aim and purpose of the cross-examining attorneys was not the discovery and elucidation of the truth but quite definitely and solely the winning of their side of the case. Hence the questions put were designed as far as possible to elicit replies which might sound bizarre and strange to technically less expert people. Apparently the best of such replies which Mr. Bennett could find are given in his seventh paragraph. These quotations from my testimony are correct and are my well-considered opinion now. However, to understand them properly, they should be read in the context of the hearings at which they were given and particularly in relation to the questions to which they were answers. Space does not permit me to supply this context here, and the reader is referred to the available transcripts of the hearings.

The Federal Power Commission's opinion, given in the eighth paragraph of Mr. Bennett's discussion, shows a complete misunderstanding of my testimony at Hartford, Conn., and misquotes it by the omission of a very important qualifying word.

Also, in this eighth paragraph, Mr. Bennett implies that the legal right of a utility to collect money for electric energy used by its customers somehow depended on the validity or invalidity of particular energy-flow postulates, presumably because some energy-flow postulate might show this energy as not deriving from some generator belonging to the utility. This is absurd. I say that my news dealer supplies me with my newspaper and pay him for it without thereby implying that he himself publishes it or creates it. In the same way I say that my light company supplies me with electric energy and pay my monthly bill, without implying or needing to be shown that the energy I consume does actually and recognizably come from some particular generator owned by my light company. That is a matter of indifference to me and, I think, has been made sufficiently clear in the last example of my paper.

In Mr. Bennett's last paragraph he implies that the potential V which I used is not the potential used by electrical engineers. This is not true. The potential V is the reading which a voltmeter would give if connected from a point in the line to the ground directly below. VI is the reading which an instantaneous wattmeter would give, if connected into and to the line at any point. The example given is one in which the assumption that electric energy flows only in conductors and is given by wattmeter readings there fails. The correcting term, which I have described in my paper and which gives a zero contribution in usual applications of electrical engineers, cannot be neglected in this example.

Roland and Jessel, who like Mr. Bennett are Federal Power Commission employees, try to distinguish between "postulated" and "measured" energy flows on a power system. By the "measured" energy flow in a line, I presume they mean the reading of a wattmeter connected to and into the line in question.

Now a wattmeter is an instrument with two pairs of terminals connecting respectively to two coils and so designed that its moving element is deflected by an amount which is a function of the average value of the product of the voltage applied to the one pair of terminals and the current flowing into the one and out the other of the other pair of terminals. If the first pair of terminals is connected, one to the line and the other to ground, and, if the second pair of terminals is connected in series relation into the line, then the wattmeter will read the average value of the product of the voltage of the line and the current in the line. About this there is no question. The wattmeter will read and thus measure the average value of VI for the line. But now when we assert that this average value of VI measured by the wattmeter is an electric-energy flow in the line, we make a postulate, which can only be justified, and that not uniquely, by showing that it does fulfill the only observable requirements, properties, or effects of a valid energy-flow postulate, namely that it does carry away from sources the proper amount of energy and bring to loads the proper amount of energy.

Now, subject to the correction developed in my paper, the VI postulate is a valid one, and wattmeters, properly connected to read VI, do measure energy flow according to the VI postulate. But Mr. Bennett has pointed out that wattmeters may also be connected to read $(V+a)I$, where $-a$ is the potential of the neutral of a three-phase system to ground. Asserting the existence of an energy flow in lines given by $(V+a)I$ is also a valid energy-flow postulate (again subject to the correction of my paper), and hence we may say that wattmeters so connected measure the energy flow according to the $(V+a)I$ energy-flow postulate.

In general, for any energy-flow postulate we may devise an instrument for measuring the quantities entering into the postulate, making the appropriate calculations with them, and exhibiting the result. Such an instrument may be said to measure the energy flow according to that energy-flow postulate. In general, for an arbitrary energy-flow postulate, the instrument will be much more complicated than the wattmeter. This, however, does not affect the principles involved in the question. The distinction which Roland and Jessel attempt to make between postulated and measured energy flows is without meaning.

In the last sentences of their discussion, Roland and Jessel reveal their understanding of the lack of physical meaning which is to be attached to questions to which different but equally valid energy-flow postulates give different answers. After describing a not particularly complicated system of several generators and several loads connected together, they ask, "From which particular source or sources is each distribution center supplied?" They anwer, "This question is a particularly sterile one to the engineer." Of course it is sterile. There is no one answer. Various energy-flow postulates, all equally valid, will give different mutually contradicting answers. They suggest that the answer will be forthcoming from administrative bodies and from courts. It is our duty as members of the engineering profession to ensure as far as possible that these bodies and courts will be technically well advised.

A New Multiple High-Speed Air Circuit Breaker for Mercury-Arc-Rectifier Anode Circuits and Its Relation to the Arc-Back Problem

Discussion and author's closure of paper 42-139 by J. W. Seaman and L. W. Morton, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, November section, pages 788–96.

Otto Jensen (I-T-E Circuit Breaker Company, Philadelphia, Pa.): The authors have made a very thorough study of and have described in minute detail the conditions which exist in rectifier installations during backfires. Their conclusions regarding the advantages obtained by using anode circuit-breaker protection are clear and obvious. The initial effort to build anode circuit breakers for rectifier protection has developed to such an extent that there are now, installed and in successful service, 188 large rectifier units protected by I-T-E breakers; 148 more units are being installed, and 175 additional breakers are in production. We then have a total of 511 units of anode circuit breakers protecting rectifiers made by all of the three manufacturers. This means that there are 1,128 poles in operation, 888 poles being installed, and 1,050 poles now in production, a total of 3,066 poles. With these figures in mind we must realize that the anode-breaker protection scheme is not new.

We note in Figure 4 of the paper that the customary high-voltage oil circuit breakers for the protection of the rectifier transformer are still advocated, and, from paragraph 3, in the conclusion, that the maintenance of the a-c power circuit breaker is reduced, because the circuit breaker is no longer required to do any work.

Our opinion may be a little biased, but we do not see any reason for installing a piece of electric equipment which has no duty to perform, particularly in times like these when our manufacturing facilities could certainly be better utilized if they were devoted to the manufacture of necessary equipment. We can however, understand the fact that, if an oil circuit breaker has no duty to perform, it is not a too undesirable piece of equipment.

We note from Figure 10 of the paper that the initial rate of rise of anode circuit 1 is approximately 6,300,000 amperes per second, and the initial rate of rise for anode 5 is 9,000,000 amperes per second. We assume that the steeper rate of rise for anode 5 is due to the d-c saturation of the transformer core caused by backfiring current of anode 1.

We do not understand the quarter-cycle delay in the reversal of the cathode current. If this is a typical condition, it further emphasizes the undesirability of cathode switching, because there is evidently a quarter-cycle delay before the cathode breaker would "know" that a backfire had occurred, and consequently the opening of the cathode breaker has been unnecessarily delayed.

Evidently, any number of backfires could have occurred up to the time that the medium-speed cathode breaker opened the circuit, and evidently the successive backfires are increasingly severe. We therefore, raise the question: Would it not be highly desirable to use a high-speed rather than a medium-speed cathode breaker in order to cut down the time interval in which sympathetic backfires could occur?

We notice the statement that the artificial backfires are more severe than the natural backfires because of the limiting effect of the arc drop in the rectifier element. This statement does not seem to be substantiated by the oscillographic record in Figure 10 of the paper, because the natural backfire in anode 5 is more severe than the artificial backfire in anode 1 both as to the amount of current and the elapsed time.

Great stress has been laid upon the design feature of the circuit breaker which prevents it from opening on any amount of forward current. We see no particular advantage in

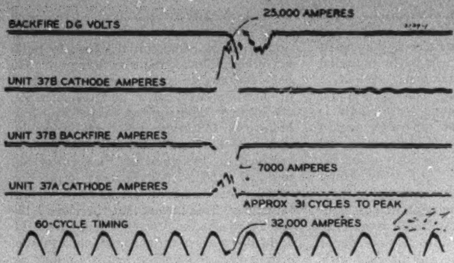


Figure 1. Backfire on rectifier 37-*B*

this. We believe a safer installation would have resulted had the breaker been deliberately designed so that it would not open up under the amount of forward current supplied by the anodes in the companion tank during backfire but would open up under forward anode current produced by the healthy anodes in case of a direct short circuit of one rectifier.

I-T-E oscillograph 7699 might better illustrate this point. A backfire was made on rectifier 37-*B*, and rectifier 37-*A* is the only other source which could feed the fault. Under this condition, the forward current which can be furnished by any other rectifier on the bus becomes the greatest obtainable from any one rectifier anode. Rectifier 37-*A* anode breaker did not trip, and this rectifier, therefore, continued to feed the d-c bus. If a short circuit had been applied to the bus, we know from a-c tests of our breaker that it would have tripped if any of the anodes had contributed more than 30,000 amperes to the fault, thereby furnishing high-speed short-circuit protection.

We cannot agree with the statement on the basic requirements that the breaker would be fully acceptable if it limited the fault current in one half-cycle. We feel that a quarter-cycle circuit breaker is more desirable than a half-cycle circuit breaker and that a tenth-cycle circuit breaker is certainly more desirable than a quarter-cycle breaker. Our oscillogram 11, published in AIEE paper 42-38 entitled "A Fast Circuit Breaker,"[1] shows the comparison between a half-cycle breaker and a quarter-cycle breaker.

Inasmuch as all the rectifier and circuit-breaker manufacturers now agree that the anode-breaker protection scheme is the most desirable, and since this scheme is based on ultrahigh speed operation, why isn't it good engineering to use the fastest circuit breaker known in the art?

REFERENCE

1. A FAST CIRCUIT BREAKER, D. I. Bohn, Otto Jensen. AIEE TRANSACTIONS, volume 61, 1942, March section, pages 165–8.

H. Winograd (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): The authors have presented a lucid exposition on the arc-back problem in rectifiers and the requirements for protection. Additional comments on this subject will be made here.

The arc-back tendency of a rectifier is related to the current and voltage rating and circuit constants by two basic factors:

1. The residual ionization in the vicinity of the anode at the conclusion of its firing period.
2. The magnitude of the negative voltage appearing between the anode and cathode at that instant. This may be termed the recovery voltage (see Figure 2 of this discussion).

At the completion of the firing period, the residual ions are drawn toward the anode surface by the negative voltage gradient, causing the flow of a small "inverse" current. The magnitude of this current depends on the density of ionization and the value of the recovery voltage. If the inverse current density at any point on the anode surface becomes sufficiently high, a cathode spot and arc discharge may be initiated. This is one of the generally accepted theories on the causes of arc-backs. Arc-backs resulting from this cause would be most likely to occur at the end of the firing period of the anode. Investigation made in one rectifier installation indicated that most arc-backs do occur at that part of the cycle. The random nature of such arc-backs is probably related to the distribution of the forward current at the surface of the anode, which would affect the density of residual ionization.

It is obvious that the higher the load current and operating voltage, the greater would be the inverse current of a rectifier and its tendency to arc-backs.

This might also offer an explanation of the effect of circuit constants such as the transformer and a-c system reactances on the tendency to arc-backs, mentioned by the authors. The commutating reactance determines the length of the commutating period for the anodes. The higher the commutating reactance, the larger is the commutating period, and the more gradual is the decay of the anode current. This provides more time for deionization, and the density of residual ionization is thereby reduced. On the other hand, the longer commutation period results in a higher recovery voltage at the end of the firing period. The two effects are opposite in their relation to arc-backs. The net effect would probably depend on the speed of deionization while the current is declining, that is, on the design of the rectifier.

It is well known that, the more the firing of the anodes is retarded, by grid or firing control in order to reduce the d-c voltage of a rectifier, the greater is the tendency to arc-backs. This also can be explained on the basis of residual ionization and recovery voltage.

The greater the retardation of the firing period, the higher is the recovery voltage, and the shorter the commutating period, which would increase the residual ionization. The inverse current would therefore be increased by both factors.

Another point of interest in connection with arc-backs, not mentioned by the authors, is the effect of the point in the cycle at which an arc-back occurs on the rate of rise of the current to the faulty anode and the value of current reached in one-half cycle. The highest value is obtained if the arc-back starts at the completion of the forward firing period. It becomes progressively smaller as the arc-back point is moved back along the reverse voltage wave. Beyond a certain point, depending on the circuit constants, the current would drop to zero, and the anode could fire in the forward direction. Arc-backs occurring during that part of the cycle might extinguish themselves without tripping the breaker. These are called "silent" arc-backs.

It would be of interest to add a historical note relating to the introduction and development of the high-speed anode breakers. Credit for this development in the protection of rectifiers is due to D. I. Bohn, electrical engineer of the Aluminum Company of America. In 1938, the first rectifiers used in this country for production of aluminum were put in operation at the Alcoa and Massena plants of the Aluminum Company. These had the conventional protective system—a-c breakers on the primary side of the transformers and high-speed cathode breakers.

In 1939, when the Aluminum Company decided to order rectifier equipment for another pot line at Alcoa, Mr. Bohn (being unfettered by tradition) proposed using anode breakers, in order to eliminate oil circuit breakers for the individual rectifier units. As engineer representing the manufacturer of the rectifiers, the writer readily agreed, since the advantages of the protection of the rectifier equipment were obvious.

As there was no high-speed breaker of this type available, Mr. Bohn proceeded to develop one himself. The writer had the pleasure of assisting him on the tests of this breaker. The I-T-E Circuit Breaker Company co-operated in this development. The rectifier equipment with the anode breakers was placed in operation in the spring of 1940.

This installation included other features relating to the design of transformers, protection, switching, layout, and cooling, which set the pattern for many subsequent rectifier installations in the electrochemical industries, as described in some of the other papers presented at the summer convention of the AIEE.

This is a tribute to the good judgment and foresight of an engineer in an important industry, and it again points to the benefits derived from close co-operation and exchange of ideas between engineers of operating and manufacturing companies.

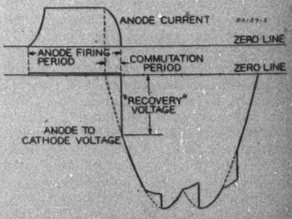


Figure 2

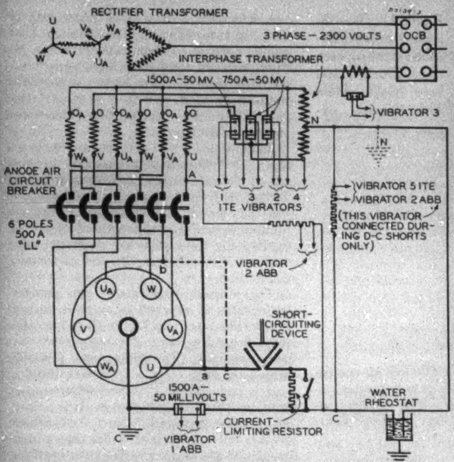

RECTIFIER TRANSFORMER
3 PHASE – 2300 VOLTS
OCB
INTERPHASE TRANSFORMER
1500A–50 MV
750A–50 MV
VIBRATOR 3
N
ANODE AIR CIRCUIT BREAKER
6 POLES 500 A "LL"
1 3 2 4
ITE VIBRATORS
VIBRATOR 5 ITE
VIBRATOR 2 ABB
(THIS VIBRATOR CONNECTED DURING D-C SHORTS ONLY)
VIBRATOR 2 ABB
SHORT-CIRCUITING DEVICE
1500 A – 50 MILLIVOLTS
CURRENT-LIMITING RESISTOR
VIBRATOR 1 ABB
WATER RHEOSTAT
U V W
U_A V_A W_A
a b c

Figure 3. Short-circuit test on I-T-E six-pole type-*LL* air breaker together with *B*-46 rectifier 625 volts, 1,360 amperes, 850 kw

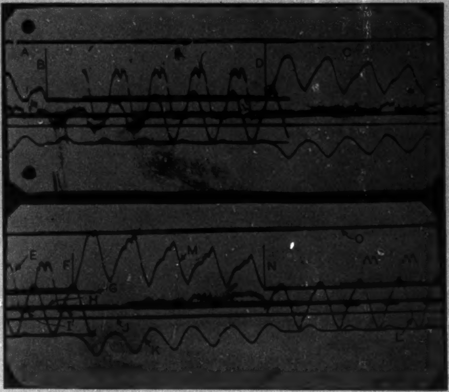


Figure 4 (above). Test of anode air breaker I-T-E type *LL*

One anode short-circuited to cathode

A—Current
B—End
C—Duration of short circuit
D—Start
E—Voltage before short circuit
F—Start of short circuit
G—Current zero
H—Voltage calibration 112 volts
I—Voltage zero
J—Primary-current calibration 5,000 amperes
K—Current in transformer primary winding
L—Voltage after short circuit
M—Current during short circuit
N—End of short circuit
O—Current calibration 15,000 amperes

O. K. Marti (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): The authors of this paper deserve a great deal of credit for having submitted an excellent outline and valuable data illustrating the arc-back phenomenon, as well as interesting design material on a new anode breaker. During the last few years considerable attention has been given to improving means for anode switching in order to minimize the effect of backfires. During the last winter convention of the AIEE a paper was presented by D. I. Bohn and Otto Jensen referring to the development of "A Fast Circuit Breaker," which has been used successfully in connection with many rectifiers.

It is felt that the following notes concerning the earlier pioneering history of anode-breaker development may also be of interest to many engineers.

Figure 5. 850-kw 625-volt automatic rectifier unit

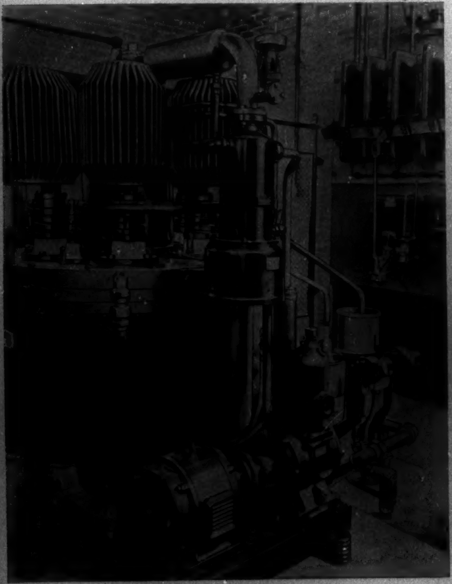


Figure 6 (below). Grid-controlled mercury-arc rectifiers rated 2,750 kw, 600 volts, direct current each, in electrolytic metal reduction plant

Anode breakers and anode reactors in background

grid current transformers
anode breakers
anode reactors

Figure 7. Two direct air-cooled multianode rectifiers, each rated 300 kw, 600 volts direct current with common six-pole anode breaker

In 1931, we were asked to prepare data in connection with the modernization of an electric-railway system, using mercury-arc rectifiers of medium capacity. During the early stage of this investigation it was found that the cost of the a-c switching equipment was quite out of line compared to the cost of the total equipment, because of the high voltage of the power circuit from which the rectifiers would have to be fed. The study showed that a very economical and satisfactory installation fully protecting the rectifiers could be obtained by using six single-pole d-c breakers for the anode leads and replacing the relatively costly a-c line breakers.

To determine the feasibility of such a scheme, the necessary equipment was set up in the factory in order to test a six-pole breaker made up from standard d-c breaker elements. The testing equipment consisted of a 3,700-kva rectifier transformer with interphase transformers, a six-anode rectifier, a short-circuiting device, and a load rheostat, and so forth (see Figure 3). During these tests the six-pole breaker was first closed and the rectifier brought up to a load of about 350 kw on a water rheostat. Then several short circuits were applied, first between one anode and cathode and second between cathode and the transformer neutral. In all of these tests the breaker interrupted in less than six cycles (see Figure 4), and the contacts showed practically no signs of pitting or burning so that the breakers could be shipped without changing the contacts. One of the installations equipped with such anode breakers and without an a-c line breaker is shown in Figure 5. Excellent service was obtained, and, considering that this breaker was not designed for such an application, the results were very encouraging.

It was not until 1939 that an occasion arose to use this scheme in a large plant. However, for this application, a specially designed anode breaker was employed.

Figure 6 shows the first installation of these anode breakers, described by Bohn and Jensen, in a plant with a number of rectifiers connected in parallel. The breakers in this installation are called on to interrupt not only the regular backfire current, but also the current component fed from the rectifiers connected in parallel. In the above case the installation consisted of 12 4,500-ampere units.

Figure 7 shows one of two recently installed substations of a 600-kw air-cooled rectifier unit which has as its only protection a six-pole anode breaker. This application of anode switching has also given excellent service for over a year.

J. W. Seaman and **L. W. Morton:** The authors are pleased to note the unanimity of agreement, that advantages obtained by using high-speed anode switching are clear and obvious. It was especially the purpose of part I, "Analysis of the Arc-Back Problem" to make a record of the reasons for these advantages. As Mr. Jensen states, high-speed anode-breaker protection is not new, but it is, however, the latest method for protection from arc-back, and, in the authors' opinion, superior to several other methods.

Use of anode switching to protect from arc-back is by no means a substitute for adequate a-c power-circuit-breaker protection of the rectifier transformer from internal faults or short circuits. Incidentally, the authors did not specify oil circuit breakers, preferring to use the broader description, "a-c power circuit breaker." Wide engineering acceptance is given to the practice of protecting any transformer of substantial rating (and many rectifier transformers are as large as 7,000 kva), by means of its own a-c power circuit breaker. Such a power breaker does have a duty to perform, and that is to protect its associated transformer from extensive damage caused by internal faults and to assure continuity of production by clearing such faults from the system instantly. It is quite conceivable that with less than adequate a-c power-circuit-breaker protection faults other than arc-back might produce damage to apparatus and loss of war production, far out of proportion to any that could be made by omitting a-c switchgear.

It is likely that transformer saturation did contribute to the higher rate of rise for the anode involved in the natural arc-back in Figure 10 of the paper. In addition, the inductance of the short-circuiting cable in circuit 1 may have had some effect. A further contributing cause may have been the exact time that the short-circuiting breaker contacts happened to close. It is believed that the contacts closed slightly after the end of conduction in circuit 1, during the test illustrated by Figure 10. A slight difference in time, even though not detectable on the oscillogram, may reduce the rate of rise and the ultimate short-circuit current. Other oscillograms in the same series of tests show higher current in circuit 1 than in other anodes involved in natural arc-back, and it is suspected that the main reason was that the short-circuit contacts closed earlier.

The statement that artificial arc-backs are more severe than natural ones and therefore err on the safe side, should be qualified by adding that the reactance and resistance in the short-circuiting cables must be kept to a minimum, and the comparison should be made using tests when the short-circuiting contacts closed before the end of forward conduction.

The authors have noted the quarter-cycle delay in reversal in cathode current in tests illustrated by Figure 10 and in other tests also. No convincing explanation has been prepared. The delays were probably caused by some obscure inductive effect.

It is true that any number of arc-backs, even up to all six anodes in the rectifier, could have occurred before the cathode breaker opened. It is also true that a high-speed cathode breaker rather than medium-speed breaker offers the maximum in protection from sympathetic arc-backs. However, actual experience proves that this is unnecessary. Seldom, with high-speed anode switching, do anodes in the other wye become involved in the arc-back. Furthermore, as is shown in Figure 10, the maximum reverse current from other rectifiers on the same bus is periodically reduced by the opening of the anode-breaker switches whose anodes are in arc-back. Finally, this limited reverse current is usually supplied and shared by all the other rectifiers so that the duty imposed on any one rectifier is far lower than its momentary overload capacity.

The feature of this breaker which prevents it from opening on any amount of forward current makes the breaker applicable to a wider variety of installations. Referring to Figure 2 of the paper, it can be seen that forward current contributed by anodes 4 and 6, reached values dangerously near such a low forward tripping current as 31,000 amperes. There will be many applications when it is desirable to trip only the one anode which arcs back and not the cathode breaker at all. Such provision preserves continuity of service by allowing the rectifier to remain in service on the remaining healthy anodes. This breaker also, as was pointed out, is designed to permit reclosing any open pole or poles, without interruption of service. This consideration alone justifies the "no trip on forward current" feature. Protection from short circuit on the rectifier is afforded by a discriminating overcurrent tripping mechanism in the medium-speed cathode breaker.

The authors agree that the faster the interruption, the better the breaker for arc-back protection up to the point where voltage surges are created. It has been observed that faster arc extinction than that employed in this breaker does give rise to such surges. Attention is called to the fact that no surges were observed during tests on the breaker described. Any improvement in speed of mechanically starting contact separation is safe and desirable, but this is not true of arc extinction.

Dr. Marti's discussion presents interesting and valuable historical notes bearing on the subject. It furnishes a good example of the efforts engineers have made to solve satisfactorily the arc-back problem in the past and shows an appreciation of the advantages of anode breakers. Many such instances could be related by the various groups who have been working with power rectifiers.

Mr. Winograd's comments constitute valuable additional data regarding mechanism of arc-back to that presented in the paper. The point raised about the effect of the time in the cycle at which arc-back occurs, influencing the rate of rise and ultimate value of arc-back current, is particularly timely. This effect was noted in connection with the whole series of tests conducted on the anode breaker and is of sufficient importance to emphasize.

Mr. Winograd is another who suggests that the anode breaker is a substitute for adequate a-c switchgear. Such a tendency seems dangerous to the authors, as it overlooks other sound and sufficient reasons for not omitting rectifier-transformer protection. The many advantages of high-speed anode switching for arc-back protection alone, set forth in the paper, surely in themselves justify the use of the breaker, and it is not necessary to resort to doubtful omissions to balance the scales.

Ignitor Excitation Circuits and Misfire Indication Circuits

Discussion and author's closure of paper 42-105 by A. H. Mittag and A. Schmidt, Jr., presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, August section, pages 574–7.

O. K. Marti (Allis-Chalmers Manufacturing Company, Wilwaukee, Wis.): This paper illustrates in a very interesting way what can be accomplished by approaching a problem step by step in a systematic procedure. It is therefore of value not only in its particular field but in a general way as well.

In a paper, "Excitron Mercury-Arc Rectifiers," presented at the winter convention of the AIEE in 1940,[1] I called attention to some of the shortcomings of the intermittent ignitor excitation scheme and discussed the continuous ignition-excitation system used in connection with the Allis-Chalmers single-anode Excitron rectifier. This paper was not very kindly received, especially the remarks regarding misfiring.

The paper under discussion, now, however gives an outline of some interesting work directed mainly toward overcoming the very disturbing phenomena to which I drew attention in 1940 and to developing means for recording these distrubances. Since my paper was presented, we have seen the introduction of a holding and relieving anode in addition to the ignitor in the ignitron rectifier, as well as a rather complicated circuit, in order to obtain faultless operation with reduced power consumption. This is the case in spite of the fact that the authors have greatly contributed to the simplification and successful operation of the ignitor excitation system. On the other hand, in connection with the Excitron rectifier using continuous ignition excitation, it was possible during the last two years to simplify the circuit somewhat, as well as the rectifier tank, since for most services satisfactory operation is being obtained with one auxiliary anode used as an ignition and excitation electrode. The circuit of this new scheme is simpler than the one presented two years ago, and the phenomenon of misfiring does not have to be contended with. In this system, if the excitation should fail, it will be automatically restored. Since the auxiliary anode is removed from the mercury, any change in mercury level or metallization of the ignition-excitation anode does not have any detrimental effect in connection with the function of the ignition-excitation system.

From the paper of J. H. Cox and G. F. Jones, "Ignitron Rectifiers in Industry,"[2] it can be seen that in connection with the ignitor rectifier a grid is located next to the anode for shielding. Such a grid is also used with the Excitron rectifier for phase voltage control, employing the well-known method commonly used in connection with multiple-anode tank rectifiers. Therefore, in order to regulate the output voltage, the ignition-excitation does not have to be disturbed as in the ignitron rectifier, which may lead to further misfiring, especially if the firing is delayed beyond the first half of the positive cycle.

REFERENCES

1. "EXCITRON" MERCURY-ARC RECTIFIERS, O. K. Marti. AIEE TRANSACTIONS, volume 59, 1940, pages 927–31.

2. IGNITRON RECTIFIERS IN INDUSTRY, J. H. Cox and G. F. Jones. AIEE TRANSACTIONS, volume 61, 1942, October section, pages 713–18.

J. H. Cox (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors indicate that the ignition impulse desired for ignitron excitation should be as steep as possible. This is true only within limits and may hold for magnetic impulse circuits since it is impossible to attain really steep impulses with such circuits. It may be undesirable to use impulses as steep as can be produced with thyratron-capacitor circuits. There is considerable evidence which indicates that, if the impulses are too steep, more instantaneous energy is applied to the ignitor than necessary, and difficulties may be encountered. On the other hand, if an ignitor becomes contaminated with surface impurities, it may be desirable to use a more sloping impulse which will apply more heating energy to the ignitor and increase its temperature. Therefore, the most desirable impulse shape is one which provides a balance between a slope which applies the correct amount of average energy and, a steepness which avoids variability in the time of ignition and a resulting unbalance between anode currents. We have found that such impulses are produced by those saturating reactor circuits which have special iron, such as Hypernik or Hypersil, in the saturating reactor and no additional reactance to further increase the length of the wave front.

The authors mention a relieving anode to take the arc current which follows the creation of the cathode spot and thus relieve the ignitor of this current. Actually, the ignitor will be relieved in any event, because after the appearance of the cathode spot any follow current will pass between the ignitor holder or rod, and the mercury and the ignitor will be shunted. Therefore, it is only necessary that the holder have sufficient area to conduct for follow current provided by the excitation circuit. With the practical construction used in large pumped-type ignitrons, this holder area is ample for any circuit that has been used. In the smaller sealed-off ignitron construction the holder area and the lead-in conductor are both much smaller and would be overloaded with the more powerful circuits. However, the smaller tubes require less ignition power, and the lower energy circuits are entirely adequate.

The autotransformer, which the authors show for the purpose of directing the successive impulses of opposite polarity to opposing ignitors and thus enable one circuit to service two ignitrons, performs the same function as the Rectox arrangement shown in Figure 4 of the paper "Excitation Circuits for Ignitron Rectifiers" presented at the summer convention of 1941.[1] The transformer connection imposes full voltage across the series Rectox, increasing its size, whereas a parallel Rectox imposes only its forward voltage drop across the series Rectox. There is probably little to choose between the two methods.

I question the statement that misleading results are obtained from the determination of circuit output characteristics by replacing the ignitor with a resistance load. The curve obtained by a variable resistance load should give an accurate determination of the maximum capacity. It frequently occurs that a circuit will operate stably with resistance load and have unstable characteristics when loaded with ignitors. This characteristic, of course, will show up in tests which include ignitors, but a circuit which is stable with ignitors can be calibrated by the substitution of a variable resistor. Also, in our recent production, excitation equipment adjusted at the factory by the use of a resistor required no further adjustments in the field.

In connection with misfires, there is some question whether the complication imposed by misfire indicators is justified. There is a great deal of operating experience which indicates that no serious consequences result from a failure of an anode to pick up, either intermittently or continuously, for either a variable type of load such as is encountered in railways or with steady loads as encountered in electrochemical service. In the design of anode balance coils it is entirely feasible to provide open-end impedance which avoids the overloading of an anode when the anode with which it is paired goes out of service. Actually, if an anode goes out of service it is indicated by several things, such as a lower voltage, lower current when paralleled with other units, noise in the anode balance coil when these are used, or fluctuating ammeter reading if the ammeter happens to be a fairly sensitive type. As the authors have indicated, it is a relatively simple matter to find the particular anode which is missing when it is known that a misfire exists in an assembly. Of course, in an automatic station some type of misfire indicator might be desirable to give indication at some distant point, and the authors have listed various satisfactory circuits that may be used.

REFERENCE

1. EXCITATION CIRCUITS FOR IGNITRON RECTIFIERS, H. C. Myers and J. H. Cox. AIEE TRANSACTIONS, volume 60, 1941, October section, pages 943–8.

A. H. Mittag and **A. Schmidt, Jr.**: The relieving anode performs the following functions:

1. Reduction of duty on dry-plate rectifier in series with ignitor.
2. Reduction of duty on ignitor in circuits with high total energy.
3. Stabilization of low energy circuits where aging of dry plate rectifier may change the circuit resistance and resulting performance.

It is our experience that a correct picture of maximum energy for ignitor firing is best obtained by use of the circuit shown in Figure 6 of the paper. In particular, the increase in firing energy in a "twin" circuit, shown in Figure 7, is not evident with a resistance-testing circuit. If the excitation circuit has considerable excess capacity, a testing method with a large margin of error may indicate acceptable operation.

The need for misfire indication equipment is not always present. Such equipment would seem desirable in certain applications, particularly in automatic stations and in certain industrial applications, particularly nonattended stations.

A 600-Volt Enclosed Limiter for Network Use

Discussion of paper 42-99 by P. O. Langguth, H. L. Rawlins, and J. M. Wallace, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, July section, pages 536–8.

Charles P. West (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Limiters comprise an essential feature of secondary distribution and industrial-network systems and provide the best-known means of selecting faults in the loop cables. Their time characteristics and fusing points, when properly co-ordinated with the cables, give isolation of a faulty conductor, without power interruption or disconnection of any sound part of the system. The authors have clearly covered the design and development of the extension of limiters from 250-volt to 600-volt service. Their places in the circuits, with the breakers and other devices, have been shown on the diagram (Figure 10 of the paper). Their physical association with other equipment should also be carefully considered, to obtain the maximum advantages of the device. When the limiters have cleared the defective cable, a new conductor must be installed. A well-planned system includes structures designed to safely allow replacement of this conductor with the remainder of the apparatus in operation.

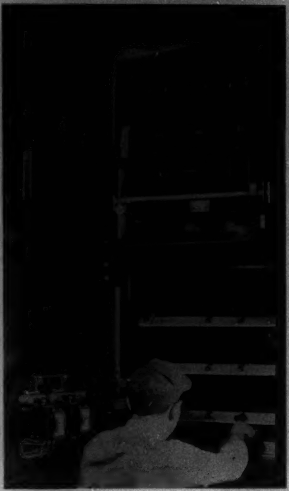


Figure 1

Figure 1 of this discussion shows part of the front of a low-voltage metal-enclosed switchgear structure designed for application to an industrial network, sections of which provide compartments for the loop-isolating switches (nonautomatic air breakers, in this case) and limiters. The limiters are normally bolted to the three-phase busses carried across the unit, and a workman is shown unbolting a limiter from the lower-phase bar. Phase isolation is provided so that the limiter can be removed safely while alive, if necessary.

Figure 2 of this discussion shows the rear of this compartment, with the three conductors of one loop cable in place and the limiters for the second cable. The lower right limiter has the insulating cover removed. The workman is holding a limiter and cover showing how they fit together. When such a structure is in operation, a faulty limiter and cable can be disconnected from the bus and drawn into the rear compartment. It will then reach into the clear space behind the gear, and the cable can be pulled out of the conduit. The new cable is pulled in and its limiter attached and connected to the bus in a similar manner.

Figure 2

Thus, proper housing and mounting of limiters provides means for replacing faulted cables in the shortest time with no loss of power to the remaining parts of the system. The use of compartments in the switchgear provides isolation for the various devices, gives a co-ordinated design, permits shipment of structures completely assembled, and makes possible all the advantages available in standardized groupings.

Electric Equipment for Large Electrochemical Installations

Discussion of paper 42-138 by T. R. Rhea and H. H. Zielinski, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and at the AIEE Pacific Coast convention, Vancouver, B. C., Canada, September 9–11, 1942, and published in AIEE TRANSACTIONS, 1942, October section, pages 733–41. The following discussion was presented at the 1942 summer convention.

W. E. Gutzwiller (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): This paper is a very timely one and covers thoroughly all phases of rectifier application to modern electrolytic plants.

It is apparent from the statistical figures in the paper that the national defense program and the war have been responsible for an unprecedented demand for power rectifiers in the electrochemical industry. This demand raised, in the short span of approximately three years, the total capacity of rectifiers installed and on order in the United States from approximately 900,000 to nearly 3,000,000 kw.

Such an enormous increase in production, on short notice and of a rather intricate piece of equipment, could be met only because some farsighted electrochemical companies had early recognized the advantages of rectifiers for electrolytic processes and had made some trial installations. The manufacturers of rectifiers were thus able to gain valuable experience and, when the defense program got under way, were well acquainted with the peculiar requirements of electrolytic processes which the authors justly state are usually of a secret nature.

The first rectifier application to an electrolytic zinc refinery was made in 1929 in a Canadian plant. Rectifiers were used for the first time in an American chlorine plant in 1934, where, since then, they have had a revolutionizing effect on cell circuit layouts, having been responsible for an increase in operating voltage from 250 to 600 and 750 volts. The year 1938 saw the first use of rectifiers in an American aluminum plant. Step by step the rectifier conquered all the electrolytic processes where electric power is an important item in the production cost. Each of these processes in the order mentioned presented new problems of rectifier application.

Because of the large amount of power required for operation of modern aluminum-reduction circuits (45,000 to 55,000 amperes at 650 volts) high-speed grid blocking and high-power high-speed rectifier switchgear had to be developed to protect the equipment against destructive fault currents.

It is only appropriate to give the Aluminum Company of America credit for having not only encouraged the manufacturers but actually initiated and taken an active part in the development of a high-speed anode breaker which is now being adopted generally by all rectifier manufacturers and operators for high-power rectifier installations.

It is interesting to note that all rectifier pioneering in the electrolytic field, as mentioned before, was carried out with the continuously excited multianode rectifier, which is still being purchased by many users on the strength of its good operating records. However, the single-anode, intermittently excited ignitron type, as well as the continuously excited Excitron type, are being chosen for many high-power installations of medium d-c voltages because of their higher efficiency and greater operating flexibility.

The authors state that continuous use of grid control should be limited to four or five per cent because of the low power factor and greater tendency to arc back. The main reason for the increase in arc-backs, which should be of interest to operators, is as follows: The more grid or igniter control is applied, the more is the firing or "pickup" point of an anode delayed; likewise, is the drop-off point at the end of the firing period delayed and shifted toward the negative half of the voltage wave. The result is that with increased firing delay the voltage between anode and cathode at the end of the firing period rises faster to a negative value, thus reducing the deionizing time of the electronic valve and increasing the reverse current to the anode caused by the residual ionization. Excess reverse current at the end of the firing period causes electron emission at the anode, a common cause for arc-backs. The effect of grid control on arc-backs at reduced load is less pronounced because of the reduced ionization prevailing under this condition. Grid control may thus be used for starting duty on electrolytic cells.

Referring to the matter of high-speed d-c switchgear, in chlorine plants, where, as a rule, one or two rectifiers are paralleled with a total bus load of 7,000 to 8,000 amperes, semihigh-speed cathode breakers with grid or igniter blocking have been found satisfactory. Likewise, installations with anode breakers and a maximum of 8,000 amperes per bus, are now being made where the semihigh-speed cathode breakers are eliminated and d-c relays tripping the anode breakers on overload are being substituted.

The authors describe how a cell or pot line is energized and de-energized by the use of igniter control. In case of rectifiers with continuous excitation, a pot line is started up by first applying blocking voltage to all grids, then establishing excitation and closing anode and cathode breakers of all units supplying the pot line. The load is then picked up by releasing simultaneously all the grids through a master contactor. A pot line is de-energized in the same manner as described in the paper, that is, by simply tripping all cathode breakers from a common trip switch.

The paper deals exclusively with the intermittently excited, ignitron-type rectifier. The application data given in the paper naturally also hold good for the continuously excited single-anode rectifier, which, like its predecessor, the multianode rectifier, maintains an auxiliary arc as long as the unit is on the line.

Interim Report on Guides for Overloading Transformers and Voltage Regulators

Discussion and author's closure of paper 42-156 by the AIEE transformer subcommittee of the committee on electrical machinery, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and at the AIEE Pacific Coast convention, Vancouver, B. C., Canada, September 9–11, 1942, and published in AIEE TRANSACTIONS, 1942, September section, pages 692–4. This discussion was presented at the Pacific Coast convention.

J. E. Clem (General Electric Company, Schenectady, N. Y.): The war emergency has focused attention upon the problem of overloading electric equipment as an aid to winning the war. The interest in this problem is based upon either a desire for immediate increased power output or upon the hope of saving critical war materials.

Whenever overloading of equipment is necessary to meet a rising war load requirement, such overloading should be done, provided that the added load does not cause the equipment to fail within the period of its vital war need. If the desire is to save critical war materials, a great deal may be accomplished by the use of new and modern system layouts, by a more accurate predetermination of the required load, and by the adoption of new and modern practices.

The output rating of transformers is based upon a definite temperature rise under specified testing conditions. The temperature rise upon which the rating is based has been selected from consideration of the maximum temperature at which the insulation of the transformer may be continuously operated and still achieve a reasonable life expectancy, together with the anticipated ambient temperature in which the equipment may operate.

The maximum continuous temperature for a reasonable life expectancy has been taken as 95 degrees centigrade; the temperature rise has been set at 55 degrees centigrade with a hot-spot allowance of ten degrees centigrade above the average temperature rise or a hot-spot temperature rise of 65 degrees centigrade. Then the condition that normal life expectancy be based on continuous operation at 95 degrees centigrade, together with the condition that the output be based on a hot-spot temperature rise of 65 degrees centigrade (average temperature rise of 55 degrees centigrade plus ten degrees centigrade allowance for hottest spot), establishes a continuous temperature of 30 degrees centigrade as the reference ambient for normal life expectancy. Estimates for the effect of varying ambient temperatures upon the life expectancy should, therefore, be made using 30 degrees centigrade as the reference ambient at which normal life may be expected.

It is pertinent to review the basis upon which the estimate of the effect of overload on the life of transformers is made. The useful life of class-*A* insulations, such as is used in transformers, is dependent upon the temperature to which the insulation is subjected. However, it should be fully realized that laboratory tests upon which estimates of the life of transformers are based are tests of a mechanical nature. In these tests, the loss in tensile strength after exposure to a definite temperature for a definite time is determined under very carefully controlled laboratory testing technique. There is a broad spread in results of such tests from different sources. The correlation between the life expectancy of transformer insulations as indicated by laboratory tests and the actual life of a transformer is largely a matter of speculation. Therefore, all estimates of the effect of overload upon the life of transformers must be tempered by sound judgment based upon operating experience.

H. K. Sels (Public Service Electric and Gas Company, Newark, N. J.): In New Jersey we have been following the procedure of operating transformers according to temperature for some time, but we have been puzzled with the practical means of instructing operators of average intelligence to handle such a technical question. In this connection, I am impressed with the data and curves presented in the interim report. In practice, short-time overloads cannot be used as such, because when the emergency arises no one knows how long it will last, nor whether a high overload should be carried for a short time, or some load dropped and a smaller overload carried for a longer time. This is particularly true when maintenance men are off duty and are difficult to get on duty to cut spares into service. Therefore, I should like to suggest to the transformer subcommittee that these technical data be put into shape to be used directly by operating personnel.

We have been experimenting with the possibility of expressing overload rating as a relationship between top-oil temperature, time, and load, so that the operator, by reading the top-oil temperature at the time of the emergency, will know from a curve the overload which can be carried. Then, as the top-oil temperature rises, the overload must be reduced until finally the overload is almost back to normal with the maximum permissible hot spot.

Another question which has perplexed us is: "When is a transformer forced air cooled?" Of course, we appreciate the fact that each manufacturer likes to install his particular air-blast equipment in his own make of transformers. However, we find that ordinary blowers or fans separated from the transformer are quite effective. Transformers are installed initially without fans, and then fans are added later. After an additional transformer is installed, fans are not needed for a period and can be used elsewhere. Therefore, the program alter-

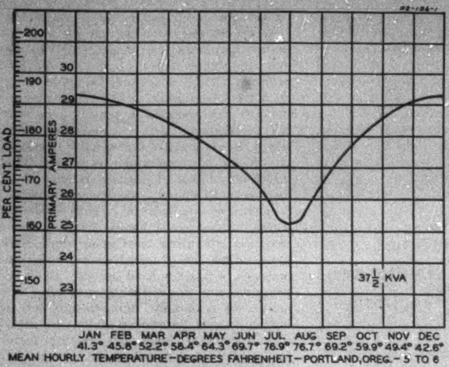


Figure 1. Maximum permissible load in per cent and primary amperes versus months of year (if not limited by voltage regulation)

A 37$^1/_2$-kva 2,400/120–240-volt single-phase overhead distribution transformer. Based on 50 per cent initial load followed by a two-hour peak period. Curve represents points at which the carrying charges on the investment and loss in life plus core and copper losses of a 37$^1/_2$-kva transformer equal the carrying charges on investment, loss in life, and installation cost plus core and copper losses of a 50-kva transformer plus carrying charges on the removal cost of a 37$^1/_2$-kva transformer. Carrying charges are 14 per cent per year. Core and copper losses amount to two mils per kilowatt-hour. Loss in life is based on study by L. C. Nichols[1]

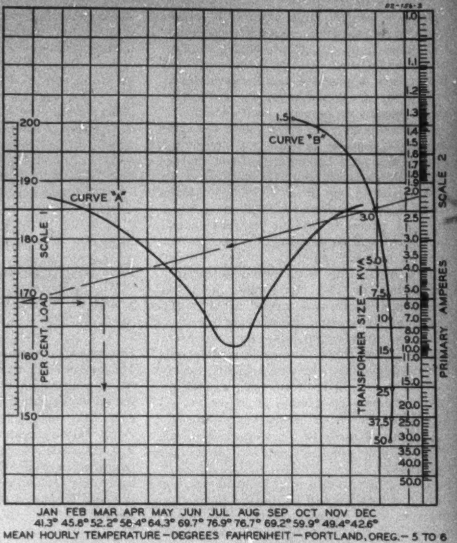


Figure 2. Maximum permissible load in per cent and primary amperes versus months of year (if not limited by regulation)

2,400/120–240-volt single-phase overhead distribution transformers. To find if 2.1 primary amperes on three-kilovolt-ampere transformers exceed maximum permissible loading in the middle of March, project a line from 2.1 on scale 2 through three kilovolt-amperes on curve *B* which indicates a 169 per cent load on scale 1. From the latter point project a line horizontally toward curve *A* to the period of the month (shown below curve *A*) in which test was made. The horizontal line from 169 per cent terminates below curve *A*, showing that maximum permissible loading has not been reached. To determine the limit of loading, project a line vertically upward from the middle of March to curve *A*, which shows 183.5 per cent or 2.3 amperes

nates between fans and additional transformers, as the load grows, so that mobile fan installations constitute the most acceptable fan cooling.

Corbett McLean (Northwestern Electric Company, Portland, Oreg.): The interim report, by the transformer subcommittee, on overloading of transformers and regulators is a pertinent one in these times of emergency when every ounce of copper and steel should be loaded to the fullest capacity.

For some time, we have been using curves on permissible transformer loading for our overhead 2,400-volt distribution transformers. A combination curve is shown in Figure 2, and an individual curve for a 37$^1/_2$-kva transformer is illustrated in Figure 1.

These curves give the maximum permissible economic loading for ambient temperatures during each month in the year and represent points at which carrying charges on investment, loss in life, and total transformer losses are equivalent to the carrying charges for the same items for the next larger size transformer plus the removal cost on the old transformer. They are based on a 50 per cent initial load followed by a two-hour peak period. Naturally, ambient temperatures, load curves, carrying charges, installation and removal costs, costs of power, and so forth, will vary considerably with other companies, and some will vary for any given company. However, the curves provide some reasonable and simple method of rapidly determining the point at which a transformer should be replaced.

During the present emergency we believe that these curves should be materially exceeded with the realization that life expectancy will be greatly reduced.

Our past experience has led us to permit a greater loading for induction voltage regulators than for distribution transformers. The carrying capacity of current transformers, cable, disconnecting switches, and so forth has generally been the limiting factor on primary feeders.

We have used water spray for some time on 5,000- and 2,000-kva single-phase 66,000/11,000-volt transformers to provide for overload capacity and have found the main drawback to be the frequent painting required on case and tubes.

We appreciate the value of this paper on transformer and regulator loading. It is most opportune and provides a standard which can easily be applied by all in the operating field.

REFERENCE

1. EFFECT OF OVERLOADS ON TRANSFORMER LIFE, L. C. Nichols. AIEE TRANSACTIONS, volume 53, 1934, December section, pages 1616–21.

C. T. Hurd (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): No one will deny that the present war emergency is going to make it necessary, in many cases, to operate transformers at an overload, but what will probably be overlooked is the premature aging of the transformer insulation from operation at these overloads.

The data brought out in the interim report showing allowable overloads are, I understand, based on the supposition that these emergency overloads will not be recurrent in nature. The basis for the report is therefore the assumption that transformers will be necessarily overloaded, but that means for relieving these overloads or for eliminating the necessity for such overloads will be promptly taken.

The term, "reasonable loss of life," is, according to my understanding, intended by the committee to mean a loss of life not to exceed one per cent, and, obviously, if an overload condition is recurrent with a loss of life of one per cent every time it occurs, the full life of the transformer insulation could be readily expended in serving the overload, unless prompt relief is provided.

Since it has been common operating practice in the past to operate power transformers at loads below their name-plate rating, and, since depreciation rates have been predicated on past operating experience, it could be easily overlooked that the cost of supplying any load, which results in overloading of power transformers serving it, will be considerably higher than normal, since the overload will result in an accelerated depreciation rate or, in other words, in a shortened transformer life. In addition, of course, other increased costs must be met, including the cost of supplying increased losses to the overloaded transformer, the effect of increased regulation of the overloaded transformer, and other factors of this nature.

Insulation in transformers does age and tends to become brittle at any operating temperature. When transformers are operated below their name-plate ratings with usual ambient conditions, insulation deteriorates at a very slow rate so that the life of the transformer is very long. Any overloads, even within the limits suggested by this report, simply mean that the life of the transformers affected will not be as great as it would have been with past normal operation, and, while it is entirely possible that no noticeable increase in the failure rate will occur during the emergency, this increased failure rate will probably have to be met later.

Transformer insulation ages by becoming brittle, and brittleness produced by transformer overload carried during the present emergency will persist throughout the entire life of the transformer. The creation of brittleness in transformer insulation obviously lessens the mechanical life of the transformer insulation, and movement of the transformer windings from any cause will be more apt to result in failure than would be the case if the transformer insulation were in normal condition. Under present conditions, overloads are doubly serious because of the rising system load factors. Overloads which follow light load conditions naturally will not result in so much loss of life as will prevail in most instances now, since most systems are operating under greatly increased normal loading. Even without the overloads, transformer copper and oil temperatures will follow a higher average value; consequently any overload which can be carried will of necessity be of shorter duration if serious loss of life is to be avoided.

It is particularly interesting to note in passing that the curves presented in this report coincide rather closely with data presented in one of the earliest papers ever written on the effect of overload on transformer insulation, namely, that by the late L. C. Nichols, entitled "Effect of Overload on Transformer Life"[1] and presented before the Institute's winter convention early in 1935. Much work has been done on transformer insulation since that date, but the figures on insulation loss of life used by Mr. Nichols are well supported by this paper.

As previously stated, it will be impossible to avoid all power-transformer overloads, but the cost of such overloads from such factors as loss of life, increased losses, and so forth, will ultimately have to be met.

REFERENCE

1. EFFECT OF OVERLOADS ON TRANSFORMER LIFE, L. C. Nichols. AIEE TRANSACTIONS, volume 53, 1934, December section, pages 1616–21.

M. S. Oldacre: We believe that the statements in the third and fourth paragraphs of Mr. Clem's discussion should be considered as his own interpretation of the American Standard *C*-57, "Standards for Transformers," in regard to normal ambient temperature, hottest-spot temperature, and life of insulation. This portion of his discussion has no bearing on the interim report, since the temperatures discussed do not appear in that report. Also we believe that Mr. Clem's interpretation of American Standard *C*-57 is not that generally accepted by the committees which prepared the standards and the interim report.

Ignitron Rectifiers in Industry

Discussion and author's closure of paper 42-118 by J. H. Cox and G. F. Jones, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and at the AIEE Pacific Coast convention, Vancouver, B. C., Canada, September 9–11, 1942, and published in AIEE TRANSACTIONS, 1942, October section, pages 713–18. The following discussion was presented at the 1942 Pacific Coast convention.

J. H. Steede (British Columbia Electric Railway Company, Ltd., Vancouver, B. C., Canada): The expansion of the electrochemical industry has provided an enormous field of application for the mercury-arc rectifier, as the authors of this paper have pointed out. It is exceedingly interesting to note the tendency toward single-anode tanks in the general field of rectifier design on this continent. This design appears to be a definite improvement over the multianode metal-tank type for all classes of application, because of the more exact control of the ignition point, the extinguishing of the arc during the time that reverse potential is applied to the anode, which reduces the likelihood of back-fire, and the lower internal voltage drop which results from the shorter arc path.

Two characteristics of the multianode metal-tank rectifier are still retained, however, namely, the necessity for water cooling with possible attendant corrosion problems, and the need for occasional pumping to maintain vacuum. Both of these features add considerably to the amount of auxiliary equipment required with metal-tank rectifier installations.

The British Columbia Electric Railway Company has had a limited experience with rectifiers for traction purposes since 1929, when a 1,000-kw 12-anode metal-tank unit was placed in service. Operating experience with this unit was disappointing. In 1936 a new tank assembly was provided, which, with close control of the cooling-water temperature, has given satisfactory service since that time.

Three more recent installations have been of the glass-bulb multianode type, consisting of two two-bulb units and one four-bulb unit, the rating of each bulb being 165 kw at 550 volts. Some data relative to these installations may be of interest.

One of the two-bulb installations has been in practically continuous service since December 1936, with only two or three short outages, caused by a-c power failure.

A similar installation made in 1937 has almost as good a service record, although this unit was put out of action on one occasion as a result of a lightning stroke on the trolley in the immediate vicinity of the substation which presumably caused the rectifier to backfire, blowing eleven of the twelve anode fuses with which this unit is equipped. These fuses were replaced and the unit returned to service without further servicing.

The four-bulb unit, which was installed in 1938, apart from occasional cleaning, has been out of service only for short periods during temporary failure of a-c power supply.

All of these units are in unattended substations, and the maintenance costs have been extremely low. One man's time for approximately two days per month has been ample for maintenance purposes, and of that time only approximately one-half day per month is actually spent inspecting and servicing the equipment.

Lysle W. Morton (General Electric Company, Schenectady, N. Y.): This paper, entitled "Ignitron Rectifiers in Industry," covers in a very satisfactory manner conventional large power installations. In general, most of the installations which have been recently made conform to the rules of application described in the paper.

However, there is one feature described, namely, water cooling by means of copper tubes soldered on the walls of the tanks, which is often accomplished otherwise.

Early experience with water-cooled mercury-arc rectifiers indicated that the direct use of most raw untreated tap waters caused corrosion of the steel vacuum chambers. Manufacturers and users immediately set about to search for a solution for that problem. Early in the 30's two satisfactory answers were found:

1. The use of noncorrosive metal surfaces in contact with the cooling water, such as copper or stainless steel. One of these the authors describe.
2. The use of sodium chromate or dichromate as a rust inhibitor.

The second method is also widely and successfully employed. A water-to-water, or water-to-air heat-exchanger system must be used, because it is necessary to recirculate the treated cooling water. The recirculating coolant is treated by adding either enough sodium dichromate to provide a 0.5 per cent solution or enough sodium chromate to make a 0.1 per cent solution.

Inspection of water-cooled surfaces of rectifiers which have been in contact at high temperature with treated waters for many years proves that it is a most successful rust inhibitor and a satisfactory application.

Still another advantage accrues from the use of sodium dichromate. Tests made on evacuated steel chambers, particularly if the temperature is elevated slightly, indicate hydrogen ion penetration of the steel envelope from either the surrounding air or from ordinary water if the chamber is water-jacketed. It was found, however, that the addition of sodium dichromate to the water eliminates this penetration and prevents contamination of the vacuum.

Using a solution of sodium chromate or dichromate as the recirculating coolant and employing the simple principle of an ordinary steel water jacket around the vacuum changer secures the following advantages:

1. A greater part of the vacuum-chamber area contacts the cooling solution, which means better heat transfer and less chance of ion penetration from the air.

2. A severely restricted material is saved.

3. The recirculating coolant does not pick up products of corrosion, such as even untreated distilled water may, from the various odd ends of the cooling system. This is true, because even though copper may be used on the rectifier and the heat-exchanger surfaces, it is usually impractical to make the surge tanks, tube headers, and reservoirs of other materials than steel or iron.

4. There is no chance of deterioration of the rectifier equipment or blocking of cooling passages as a result of corrosion or products of corrosion.

It is sometimes desirable to ground the water-to-water heat exchangers to eliminate the inconvenience to personnel of having the heat-exchanger surfaces at various potentials above ground. This can be done both with the soldered-on copper tubing employing distilled water, or by using 0.1 per cent sodium-chromate solution, as the leakage currents are satisfactorily limited. However, if it is not too inconvenient, it is better to insulate the heat exchanger from ground and allow it to operate at rectifier potential. This has the advantage of keeping the leakage currents back to ground in the cooler and more highly resistant part of the water circuit where they can be minimized.

H. K. Sels (Public Service Gas and Electric Company, Newark, N. J.): Cox and Jones have dealt rather briefly with the problem of telephone interference. There are many other factors involved besides those mentioned, such as the susceptibility and configuration of the communication or signal system and the present influences and electrical characteristics of the power system. This problem cannot be dismissed lightly considering the inductive co-ordination measures usually present in every system where some additional influence such as rectifier harmonics may produce trouble.

The suggestion of the authors that 36-phase operation gives satisfactory operation in most cases raises two questions:

1. Speaking in general terms for average conditions, what proportion of system load can rectifier load become before trouble will be experienced?

2. Have there been any cases where more than 36-phase operation has been required?

This is a question involving several parties; namely, the utility, their customer, communication or signal interests, and equipment manufacturers. From a utility standpoint, it is expected that the customer and the manufacturer of his equipment will be responsible for continuing good relations with communication and signal companies.

J. H. Cox: Mr. Steede has discussed glass-bulb mercury-arc rectifiers and has presented a case where they have proved satisfactory. However, it must be admitted that these large glass bulbs are inherently fragile wherever installed and are suitable at all only for relatively low-power installations. For large installations such as are common in the electrochemical industry the glass bulb has two very serious limitations:

1. The current per anode is low, and the number of main and secondary wires would be prohibitive.

2. On large busses high-speed switching is necessary to avoid destruction of apparatus, and the cost of high-speed switching would be prohibitive for even the largest glass-bulb rectifiers.

Mr. Morton has discussed the use of ordinary steel water jackets on the rectifier, together with sodium-dichromate treatment of the recirculating water. It is true that this system is also quite satisfactory. I question the statement that there is hydrogen-ion penetration through the steel where exposed to air. Such penetration would depend upon some corrosion taking place, and, obviously, an exposed portion of the tank would be finished to prevent such corrosion. The only functions of cooling are to dissipate the losses and to maintain a mercury vapor pressure which provides satisfactory rectifier performance. Since this performance actually is highly satisfactory with soldered-on copper cooling coils, there can be little argument about the effectiveness with which such a system cools the tank walls. Furthermore, where copper is used on the rectifier the extent of corrosion that takes place is purely nominal in a recirculating system where oxygen is excluded, even on the portions of the circulating system that must still be made of iron. It will be agreed generally that the grounding of a piece of apparatus is always desirable, and the sodium-dichromate treatment of the recirculating water certainly increases the difficulty of grounding the heat exchanger.

Mr. Sels discusses the question of harmonics generated by large rectifier installations and asks what proportion of system load can rectifier loads become before trouble will be experienced. There are several installations where the rectifier load constitutes practically the entire generating output. In at least one case this local system, involving over 100,000 kw, is connected to a public-utility transmission system, and no difficulty has been experienced. In the Bonneville territory the rectifier load is becoming a very important percentage of the total. Although no serious difficulty has been encountered, the sheer magnitude of the powers involved in this location has caused at least one user to decide to increase the number of phases to 108 at those stations where this is possible.

Three-Winding Transformer Ring-Bus Characteristics

Discussion and authors' closure of paper 42-153 by G. W. Bills and C. A. MacArthur, presented at the AIEE Pacific Coast convention, Vancouver, B. C., Canada, September 9–11, 1942, and published in AIEE TRANSACTIONS, 1942, December section, pages 848–9.

A. H. Frampton (The Hydro-Electric Power Commission of Ontario, Toronto, Ont., Canada): The type of switching diagram proposed by the author is one which, in slightly different form, has been proposed for several major developments in eastern Canada. The selection of a proper diagram in such cases is often difficult, and it has been found helpful to draw up the following specification for an ideal diagram against which alternative proposals may be compared, and their relative merits weighed. Probably no one diagram will ever meet all of these requirements, but at least the exercising of personal judgment may be limited to the weight accorded each item in the specification.

1. The switching diagram should be sufficiently flexible to permit the available capacity being subdivided into as many components as there may be independent groups of load to be supplied, while retaining:

(*a*). Convenient and flexible control of real and reactive power flow in each load subdivision.

(*b*). The ability to isolate any piece of equipment, in event of disturbance or for purposes of routine maintenance, without affecting other equipment which should remain in service.

At the same time the highest practical efficiency of operation should be effected in the combined plant, whether measured in terms of utilization of river flow or fuel consumption.

2. The clearance of any disturbance in any one load subdivision should not affect the continuity of service in other load subdivisions.

3. The loss of one transmission circuit in any one load subdivision should not isolate from the load generating capacity in excess of the operating reserve capacity in that load subdivision.

4. The operating duty imposed upon both new and existing circuit-breaker equipment should not exceed the safe capacity of such equipment.

5. The diagram should be simple, preferably symmetrical and of a repetitive form, so that the switching connections in each of the load subdivisions will resemble each other and also the combined station diagram. Undue complexity that would hamper the operating staff, particularly under emergency conditions, should be avoided.

6. The over-all cost should compare favorably with alternative proposals.

As the diagram shown in Figure 2 of the paper is presumably not advanced as a practical proposal, but only as representing any form of diagram resulting in bussed connections at the sending and receiving ends, no comments are offered beyond the fact that a saving in low-voltage bus copper, and some reduction in short-circuit values would result from the use of conventional double low-voltage windings in the step-up transformation. However, if the diagram proposed in Figure 1 of the paper is judged on the basis of the above specification, it will be noted that it will rank high only if it is granted that the station will always supply only one load group. In that case, all but two of the conditions will be satisfactorily met, the exceptions being that all breakers are inaccessible for maintenance work without the associated equipment being taken out of service, and the over-all cost is likely to be high.

In the latter connection, the author refers to the additional cost of the increased step-up transformer capacity as being partially offset by the elimination of high-voltage breakers. Actually, if it is granted that in Figure 2 double low-voltage windings could have been shown, then the author's diagram requires at least double-capacity breakers and connections in the low-voltage transformer circuits, with a further consequent increase in cost. Approximate estimates would appear to indicate that the total increased cost could be offset only if the transmission voltage were considerably higher than 230 kv, thus effecting a very substantial saving in high-voltage switching. In the optimum case, it is possible that the author's proposal might result in the saving of one complete transmission circuit.

On the other hand, if it is required that the station illustrated be subdivided into two load groups, or say a nine-unit station into three groups, it will be noted that it will be extremely difficult to satisfy the requirements of condition 1 in the preceding specification, even though additional connections are added to form further loops in the low-voltage circuits. If these connections are added, then the diagram becomes quite complicated, conflicting with condition 5. It is considered therefore that this form of diagram would find application only in single-load group generating stations or their equivalent.

H. N. Muller, Jr. (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors have described the results of an a-c network-calculator study performed to investigate the merits of a particular generator bussing scheme. This ring-bus arrangement was studied over 15 years ago at the time the Boulder Canyon project was under consideration, but results of such studies have never been presented in the technical literature. The investigation performed by the authors shows that application of this bussing arrangement results in increased transient power limits and decreased short-circuit currents, both of these attributes being of prime significance on certain systems.

The authors chose for study a ring-bus system composed of four groups of two generators connected through three-winding transformers to four high-voltage lines. All these lines terminate on one load bus. Since each generator is specified to have a capacity of 55,000 kva, this means that the total output of the assumed station, 440,000 kva, is transmitted to one load center. This is not improbable, but the more usual case would be for these transmission lines to carry the station output to two or more load centers. In the introduction to this paper, power concentrations of over 1,000,000 kw are directly referred to, and in developments approaching this size it would be extremely unlikely to have the entire output delivered to one load bus. As a matter of interest, the authors name Boulder Dam, Grand Coulee, and the proposed St. Lawrence project as exemplifying large power concentrations where the bussing scheme studied might be of advantage. Note that none of these projects named do, or probably would, deliver their total output to one load center.

The question then naturally arises as to whether the smaller ideal system chosen for study by Bills and MacArthur can be used to reach conclusions that can be extrapolated to apply to the larger and more general layouts. With this point in mind the discusser has performed additional studies of a more general nature, concerning the results of which brief comments are offered.

If a larger generating source is assumed than that chosen by the authors, and if the transmission lines emanating from this source are of varying length and deliver load to two or more independent load centers, the three-winding transformer ring-bus scheme becomes less ideal. Significantly higher transient-power limits and lower short-circuit currents still prevail, but other operating limitations can appear. If one independent load center is drawing heavy load, while the load supplied over other lines to another receiver is light, unbalanced division of the reactive kilovolt-ampere burden between generators will result, and one or more generators may exceed their maximum field limitations while other units operate with light field current. To alleviate this difficulty it would be necessary to sacrifice bus voltage regulation. If it is desired to control the voltage of the transmission lines feeding one load center independently of those lines feeding another receiver, the proposed ring-bus arrangement appears inflexible compared to certain more conventional schemes.

The above points are not mentioned with an intent to condemn but are raised to show that the bussing scheme in question requires careful study before its use is recommended for a particular installation. Very broad conclusions cannot be drawn from study of one system. On systems embodying the comparative simplicity of the one studied by the authors, this transformer ring-bus arrangement appears to offer valuable advantages without introducing any difficult problems of operation. It deserves serious consideration on projects whose complexity is limited by size but of necessity must be carefully analyzed on large projects, since the operating problems and limitations which can result may overbalance the inherent advantages.

G. W. Bills and C. A. MacArthur: It is granted that the three-winding ring-bus arrangement is not a cure-all, but no known system will satisfy all the requirements specified by Mr. Frampton. To bus as much as 1,000,000 kva of generators at a station is not only risky in that the entire station may be lost during a disturbance, but problems involving the control of real and reactive power flow may arise when different load groups exist. For this reason we consider only single-load systems although more extensive systems have been investigated and found feasible.

Mr. Frampton stated that only the use of transmission voltages higher than 230 kv would offset the increased cost of larger transformer capacity. We would like to point out that it is permissible to invest more in the three-winding transformer ring-bus system than in the sandard system because of the increase in firm power it is possible to transmit. However, an economic study was beyond the intended scope of this paper.

From transient-stability and short-circuit considerations, however, the three-winding transformer ring-bus system has been shown to have very definite advantages.

It is extremely interesting to know that Mr. Muller investigated the ring-bus arrangement years ago and arrived at the same conclusions as the authors concerning the stability and short-circuit considerations.

We would like to point out that the output of six 82,500-kva generators at Boulder Dam sends power to a common bus at Los Angeles; that in the proposed St. Lawrence project as much as 500,000 kw may go to Rotterdam, N. Y.; and in the Bonneville Power Administration transmission system similar blocks of power will be transmitted to concentrated load centers such as at Seattle, Spokane, and Vancouver, Wash.

Any scheme which busses all the generators will be inflexible when it becomes necessary to regulate different receiving-end bus voltages by changing the sending-end potential. However, most modern hydroelectric projects resort to the use of synchronous condensers and transformers having tap changing under load for purposes of regulation so that this is not a serious problem.

Mr. Muller's point that this ring-bus arrangement must be carefully analyzed for a particular system is well taken. We did not intend to imply this bussing arrangement was suitable for *all* large systems but had hoped to show it might be very suitable for certain projects.

A Practical Discussion of Problems in Transformer Differential Protection

Discussion and author's closure of paper 42-146 by P. W. Shill, presented at the AIEE Pacific Coast convention, Vancouver, B. C., Canada, September 9–11, 1942, and published in AIEE TRANSACTIONS, 1942, December section, pages 854–8.

John Teasdale (British Columbia Electric Railway Company Limited, Vancouver, B. C., Canada): There are two paragraphs near the beginning of this paper which, in generalizing on current-differential schemes, seem to suggest that various measures used to secure good balance on differential circuits no longer have the same value as formerly. This view is scarcely in accordance with the facts.

As experience is extended, and as larger units come into service, the trend is rather in the direction of still greater care in the application of current-differential protection and instances to the contrary imply the acceptance of a lower standard of engineering.

Accuracy in reproducing the primary conditions in the secondary circuits at all times is still the ideal sought after and is approached as nearly as possible on important installations. When all that is possible has been done in this direction, *then* consideration is given to dealing with inaccuracies that cannot be avoided, so that they do not affect relay operation adversely.

A high-setting instantaneous relay combined with a low-setting timed relay, as referred to in this paper, is one method of evading trouble on account of these causes, but it should not be regarded as modifying the need for a good balance in the secondary circuits. Otherwise, to be entirely safe, it will require settings that involve material sacrifice in speed or sensitivity. For this reason, its value on installations of any importance, without any additional protection, is likely to be inferior to ordinary differential protection using restraint on through faults, and these relays cost little more and are probably no more difficult to obtain in wartime.

E. G. Ratz (Canadian Westinghouse Company, Ltd., Hamilton, Ont., Canada): Induction-type relays having very inverse characteristics, with fairly sensitive current settings and a high-speed instantaneous trip device included, are difficult to improve upon for transformer differential protection.

The maximum through-fault current can usually be determined from the reactance of the transformer bank, and the difference currents in relays can be reduced to a minimum on through faults by exact matching of the current-transformer ratios. We have found this to be a very desirable procedure in order to make such difference currents a minimum and thus reduce this limitation to the permissible sensitivity setting. If proper care is taken, difference currents on through faults will certainly be small, and, even if they do exceed the current setting of the relay-induction element, faulty tripping will almost certainly be avoided because of the difference currents being such as to cause the relay to operate on the inverse and longer timed portion of its curve.

Difference currents on through faults are also minimized by keeping the burden on the main current transformers at a minimum, and this makes the omission of auxiliary balancing transformers desirable, as such transformers usually have a relatively high burden.

The current setting of 15 amperes and 15 cycles suggested for magnetizing inrush seems high. Even if the maximum inrush current considerably exceeds the current setting, this current falls very quickly, and a very inverse characteristic will prevent faulty operation. Further, relay elements of the induction type are less susceptible to difference currents with d-c components than plunger-type relays.

With respect to the author's objection to the use of suppressor devices to prevent operation on magnetizing inrush, it is suggested that a relay of the ratio differential, or any other type, can have restraint added with a momentary compensating magnetic characteristic in proportion to the *rise* in voltage. This restraining action would be the maximum at the moment the transformer bank is energized and would die out at approximately the same rate as the magnetizing inrush, thus providing restraint only when required and approximately in proportion to the requirements at various moments during the magnetizing inrush period. If the transformer bank were picked up with a fault, the fault-current torque would exceed the restraining coil torque within a cycle or two, providing the required operation without undue delay.

We have used impedance relays, with the current coils connected in the differential circuit, and the voltage coils energized from the main circuit and set to operate instantly at a differential current of approximately three times full load current, at full voltage. In case of bad faults, the voltage drops, thus reducing the differential current required to trip, in proportion to the drop in voltage. This relay is more sensitive on faults than a straight instantaneous overcurrent relay and is particularly effective and fast if the transformer bank is large compared to the connected system, for example, if the fault current delivered to the transformer bank is unusually low because of reduction of connected capacity.

We feel that it is highly desirable to provide devices to limit the sensitivity momentarily to meet inherent conditions such as magnetizing inrush, and through-fault currents, if necessary, rather than to reduce the sensitivity of the equipment under all conditions.

L. L. Fountain (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The subject paper calls attention to some very important considerations in the differential relaying of transformer banks. The admonishments in regard to washing out zero-sequence currents, where the transformer bank is grounded, and the methods used in calculating ratio of current transformers are adequate.

I feel, however, that not enough emphasis has been placed on the calculations necessary to obtain the transient values mentioned under the heading "Relays." In order to arrive at the assumed values given for maximum differential current caused by through- and magnetizing-current transients, a rather complicated set of calculations must be made, requiring a knowledge of the current transformer's saturation characteristics, time constants of the system, and relay characteristics when energized by transients containing a large d-c component. Probably Mr. Shill considered that the practical nature of his paper did not permit delving too much into these calculations. Mention of them, however, would prevent any false notion that the values he gives are readily available on the majority of applications.

The percentage differential relay with restraint windings is certainly to be preferred over and above the simple overcurrent relay. This type of relay permits the use of current transformers that are not evenly matched so far as saturation characteristics are concerned. Also the complicated calculations necessary in the application of the simple overcurrent relay can generally be omitted when the percentage differential relay is used, since its restraint characteristic allows a larger differential current to flow without false operation.

The percentage differential relay used in transformer protection is of the same construction as the induction time-delay overcurrent relay recommended in this paper. Because of its popularity and the similarity of its parts to those used in the induction overcurrent relay, it can be obtained from the relay manufacturers as readily as any other relay.

The discussion of magnetizing-current inrush trip suppressors seems to deal only with those types that depend on time delay or shunting the relay operating coil to prevent false operation. There is available a type of trip suppressor that has none of these undesirable characteristics but depends on system voltage indication as a monitoring means to prevent false relay operation. A breaker closing to energize a transformer bank normally applies full voltage across the windings. If these voltages are normal, then the trip suppressor prevents the differential relay from operating on the magnetizing-current inrush. Should the transformer be faulted, then one or more of the three-phase voltages will be below normal, and the trip suppressor will allow the differential relay to function in a normal manner without time delay or decrease in its sensitivity.

S. Minneci (General Electric Company, Pittsfield, Mass.): In his discussion of the problems in transformer differential protection Mr. Shill describes a practical solution for the protection of power transformers with *fixed* ratios. Transformers equipped with load ratio control have their ratios changed by taps in any of their windings. A *variable-ratio* transformer will therefore allow an unbalance of 10 to 20 per cent in the secondary currents of differentially connected current transformers, in its leads. Since the unbalanced current thus produced does not increase greatly as a percentage of the through current, percentage differential relays in which the operating current is a certain percentage of the through current are usually employed to obtain a sensitive setting at low currents without danger of tripping the transformer breaker on through faults. This problem was the subject of an article written by J. W. Dodge and published in the May 1929 issue of the *General Electric Review*.

Another consideration in the problem of differential relay protection is the effect on the duration of inrush currents when transformers are paralleled. Studies made by C. D. Hayward, relay engineering department, General Electric Company show that inrush currents appear in both the transformer which is to be energized and the transformer already in service and that the inrush currents may persist at nearly full-load current magnitude for a period of 66 cycles after the second transformer is connected in parallel. Factors affecting the inrush currents and a means for adequate protection under these conditions are described in a paper prepared by Doctor Hayward.[1]

REFERENCE

1. PROLONGED INRUSH CURRENTS WITH PARALLEL TRANSFORMERS AFFECT DIFFERENTIAL RELAYING. C. D. Hayward. AIEE TRANSACTIONS, volume 60, 1941, pages 1096–1101.

P. W. Shill: While the paper was intended originally to deal with the subject of the present exigencies and the limitations in the selection of relays and other equipment which have been imposed on protection engineers by our governmental regulations, the discussion has tended more to the consideration of ideal conditions and designs.

The exact matching of current transformers to suit the inherent ratio of the power transformers presupposes that there be a series of taps on one set of current transformers to match the taps on the power transformers for the ratio-adjusting or tap-changing switchgear. While it may be quite easy to make changes in the cases of off-load ratio adjustments, it would be necessary to provide tap-changing switchgear for the current transformers in the case of automatic tap changing on load. This is hardly feasible economically; neither is it necessary, because small permanent differential currents can be taken care of easily. Slight differences in current-transformer ratios are no more serious in the cases of transformer banks with fixed ratios than they are in the cases of those with automatically variable ratios.

It is true that auxiliary current transformers usually have rather high burdens; in fact, most of them have burdens which are considerably higher than they need to be. The errors they may intoduce are most serious in the cases of through-fault currents. In these cases the burdens of auxiliary current transformers are shared by two of the sets of power current transformers in the differential system. While the portions of the total burden of the auxiliary current transformers which are carried by each set of power current transformers may still be high enough to impair the ratios of the latter, both sets will be subject to the same conditions so that any differential between their secondary currents under these conditions will be relatively small. The chief objection to auxiliary current transformers is that they may provide additional d-c transients under fault conditions. The relays must be arranged to pass these transients. In the case of faults in the differentially protected zone, the auxiliary current transformers, by upsetting the ratio of one set of power current transformers, tend to make the protection more sensitive.

The calculations of expected transients were considered to be outside the scope of this paper, and they have been dealt with very well by other authors at different times. It should be noted that the figures given in the paper cannot be considered as typical by any means. Each case must be carefully considered on the conditions to be met, if there be any uneasiness of mind about transient conditions to be expected, or experience proves that such transient currents do reach troublesome values in any particular case. The case reported verbally at the convention meeting of a transformer bank having a charging current inrush of five times normal full-load current and requiring 2.5 seconds (150 cycles) to decay, shows what can be expected in extreme cases.

There is no argument that restrained relays which drop their restraint under fault conditions in the protected zone are very desirable and should be used if possible. From the writer's experience, current-ratio differential relays requiring as high as a 50 per cent unbalance to operate are not so satisfactory for passing transient currents as are ordinary overcurrent relays.

With regard to the availablility of relays, the writer has just received three ordinary induction-type overcurrent relays with instantaneous attachments for which the order was placed over 11 months ago. Of course, the standard explanation is "priorities." This may be sufficient and quite satisfactory for the devotees of this new quasi religion, but it does not help the man with a job to do, who has to meet dates when apparatuses are required to be put into service. How long it would take to get any special types of relays is anybody's guess these days.

Anyone who has had experience in practical engineering design knows that in almost all cases, in all branches of engineering, pinpoint accuracy seldom is finally attainable or necessary. After one has made the calculations for one's ideal design, one must immediately look around for the equipment and materials which most closely meet the ideal requirements. Equipment and materials usually have to be selected from standard sizes and available stocks and adapted for one's purposes. Special equipment is, usually, relatively quite expensive and is not often justified by engineering considerations. If one accepts the principle that lack of pin-point accuracy is proof of a low standard of engineering, then there is and has been very little high-standard engineering done in any branch of the profession. In the writer's opinion, high-standard engineering consists of:

1. Determining the ideal arrangement and conditions that are desirable.
2. Determining the tolerances that are permissible.
3. Selecting the equipment and materials which are most available and economical for the purpose.

Pin-point accuracy is no more necessary in a properly designed current-differential protection than in any other piece of engineering.

Method for A-C Network Analysis Using Resistance Networks

Discussion and author's closure of paper 42-149 by Waldo E. Enns, presented at the AIEE Pacific Coast convention, Vancouver, B. C., Canada, September 9–11, 1942, and published in AIEE TRANSACTIONS, 1942, December section, pages 875–80.

Mabel M. Rockwell (Lockheed Aircraft Corporation, Burbank, Calif.): The author of this paper deserves commendation for his ingenious application of the circuit theory to the solution of the always difficult network problem using the d-c board. This development should be of considerable practical value to utility companies, since most of them have d-c boards available for short-circuit studies, but comparatively few have a-c boards, and under present conditions the likelihood of obtaining such is undoubtedly remote.

In operating a power system practical questions of load distribution, substation location, and so forth, occur even more frequently than relay problems and in the past have often had to be solved by very approximate or laborious methods. While Mr. Enns' method is, of course, more laborious than the simple solution of using an a-c board, in the absence of the latter it presents a powerful and useful tool for solving many urgent problems with the equipment on hand.

V. B. Wilfley (Westinghouse Electric and Manufacturing Company, Seattle, Wash.): The author of this paper should be commended very highly for his development of the method he has described.

Heretofore, it has been considered necessary to use the a-c calculating board for the making of complicated system load studies. However, there are few such boards available, and the means of making use of the standard variable resistance d-c board, as described by Mr. Enns, is of decided importance.

In these times, the very best possible use of all system facilities is necessary, and load division studies are essential for best system operation. Practically every utility owns a d-c board, and a description has been given as to how it may be used to obtain valuable information.

In the past, the use of d-c boards has, generally, been confined to short-circuit studies. Even for this type of study, the results were subject to some error, because either the resistance had to be considered as negligible or, if impedance values were used, those in one circuit had to be assumed to be inphase with those of other circuits. The method covered by the paper will be much more accurate for all types of studies, as the resistance and reactance are used in their correct relationship to each other.

A review of the paper indicates that a certain amount of paper calculation work is necessary, but these calculations seem quite straightforward and involve only simple slide-rule mathematics. Also, such studies are not made frequently of a general rule.

In the first part of the paper, reference is made to a design of d-c board which will be particularly well suited for use with this method. It would be appreciated if Mr. Enns would give a little additional information about its general features and how it differs from the standard d-c board. I assume that it will make use of four circuits connected in series parallel to save time in obtaining readings.

Charles A. MacArthur (Bonneville Power Administration, Portland, Oreg.): One difficulty with Mr. Enns' proposal is the necessity for estimating voltages at points removed from the reference bus. The initial estimate should be relatively close in order to avoid the necessity for a second solution with the four network combinations (real and reactive current with resistance and reactance networks). For networks which are radically changed from any previously studied, this may be very difficult.

A short cut to obtain this initial result is possible by using the real component of current with the resistance network and the reactive component of current with the reactance network. These two combine to give nearly the correct magnitude of voltage drop to remote busses. The bus voltages thus obtained may be used as described by Mr. Enns in section 3 of the "Application of Method to Network Problems."

The use of resistance networks for a-c network analysis is a valuable tool for companies which cannot justify an a-c calculating board.

W. W. Parker (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Mr. Enns' paper is very interesting in describing an ingenious method of overcoming the inherent limitations of a d-c calculator board in the study of a-c networks. By successive solutions of four equations with four separate setups, the real and reactive power flow and bus voltages can be obtained.

As indicated in the paper, wherever a-c network calculators are available, they will always be used in preference to this method, as obviously the direct replica approach gives accurate solutions much more quickly and easily. For other than short-circuit or the simpler regulation problems, that is, for transient-stability and load-control interconnection problems, the application of the method discussed in the paper would be decidedly limited.

In the last few years, as outlined in the recent paper, entitled "The Modern A-C Network Calculator"[1] which was presented at the 1941 Pacific Coast convention, the number of a-c network calculators has greatly increased. The latter two, mentioned at that time as being under construction, have since been installed and are now in practically continuous operation.

Many of the a-c network calculators are available for use by engineers other than the owners, so that it is now possible for a-c calculator analysis of any of the major problems of the design and operating engineer.

REFERENCE

1. THE MODERN A-C NETWORK CALCULATOR, W. W. Parker. AIEE TRANSACTIONS, volume 60, 1941, November section, pages 977–82.

Waldo E. Enns: The discussions presented indicate the acceptance of the method outlined in the paper as a practical tool for making load distribution and voltage studies with a d-c calculating board. This favorable opinion is appreciated, coming as it does from engineers close to the subject of a-c network analysis.

One point brought out in the discussion is that the use of this method applied to existing d-c boards is more laborious than the a-c calculator. This point must be admitted; however, where an a-c calculator is not close at hand, in many cases load studies can be solved more economically on an existing d-c board.

Although it was beyond the scope of the present paper, the writer hopes to describe soon a resistance board designed especially for this new method which greatly simplifies its use.

A New Single-Pole Service Restorer

Discussion and author's closure of paper 42-147 by E. E. Tugby, presented at the AIEE Pacific Coast convention, Vancouver, B. C., Canada, September 9–11, 1942, and published in AIEE TRANSACTIONS, 1942, December section, pages 889–92.

G. F. Lincks (General Electric Company Pittsfield, Mass.): Mr. Tugby has described a very interesting development in restorers or reclosers. One of the features of the restorer is the adjustability of time-current characteristics in the field, so as to trip at 150, 200, or 250 per cent of the rating within a variable time range of from about 2.5 to 9 seconds. While apparently desirable, there are some practical application problems which make the value of this feature somewhat questionable.

It is unlikely that many operators, even if they could, would consider throwing short circuits on their distribution systems in order to make the field adjustment Mr. Tugby states was necessary in properly coordinating the system described and shown in Figure 5 of the paper. In making system co-ordination studies for overcurrent protection, it is customary to demand a very high degree of reliability and accuracy in both the published time-current characteristics and the product. This permits making a completely co-ordinated system setup on paper with the expectancy of securing 100 per cent successful operation when installed. For example, a number of utilities with more than four years operating experience on sectionalized systems protected by single element and reclosing fuse cutouts report that they have yet to experience one false operation.

To make use of the adjustment feature of the restorer described for making a co-ordinated system setup on paper, it would be necessary to have a series of time-current curves such as is provided for relays. The time-delay adjustment of each restorer would have to be accurately calibrated for a number of intervening settings between the minimum and maximum so as to meet curves published for each one. Such a procedure naturally would involve some variables in commercial manufacture which would have to be known so they could be taken into consideration in making co-ordination studies. It would be of value if Mr. Tugby could give some data on just how much this variation would be. A comparison of the curves of Figure 2 of the paper when replotted on log-log co-ordinate paper shows a difference in shape between the maximum and minimum time delay. This would indicate the need for individual curves for each adjustment.

In setting up a co-ordinated system it is customary to start with the protective device farthest out on the system. The economic justification for the use of restorers or reclosers would be premised on services feeding appliance loads, such as ranges, and so forth, requiring at least 60-ampere service entrance fusing. As pointed out in the AIEE paper "Trends in Distribution Overcurrent Protection,"[1] in order for 60-ampere 250-volt fuses to operate ahead of the fuse on the primary circuit of the distribution transformer, the primary fuse link must be rated at least three amperes at 7.5 kv, five amperes at five kv, and 15 amperes at 2.5 kv. A majority of the restorers will be used with a minimum three-ampere fuse rating on 7.2/12.5-kv grounded-wye circuits. Thus, for example, proper sequence of operation with the 60-ampere service entrance and five-ampere primary fuses will require setting three-ampere restorers to trip at 250 per cent of the rating and with the maximum time delay. Progressive heating during the two subsequent closures cannot be depended upon to cause a higher rated fuse link to melt before lockout, since the seven seconds reclosing time of the restorers would permit too much cooling of this link. Some advantage might be gained from the adjustable feature if the restorers closer to the source of supply could be made to operate more slowly than the setting of the three-ampere restorer. But if the restorer at the extreme end is set at or close to the maximum tripping current and time delay, the desired number of restorers to be connected in series may restrict the permissible lower setting of the restorers closer to the source of supply.

The preceding illustration uses the three-ampere restorer as an example. However, other factors such as a "minimum fusing practice of five or eight amperes" at the transformer primary winding would preclude the use of a three-ampere restorer. Instead, a six-ampere restorer would be required with minimum 200 per cent and 250 per cent current pickup for five- and eight-ampere fuses respectively and with the maximum time delay. Transformers with internal fuses, especially the earlier product, might necessitate adjusting even 20- or 25-ampere restorers for maximum current and time-delay settings. Service experience has proved repeatedly that long-time outages amounting to as high as 12 or more hours may be anticipated if the transformer fuses and line-sectionalizing equipment are not co-ordinated properly.

In addition, economic considerations may dictate the use of single-element fuse cutouts at the branches. At many such installations fuse links will be employed which are rated higher than those just considered for transformer protection, thereby affecting the settings of higher-rated transformers. The AIEE papers, "Relative Value of Different Types of Overcurrent Protection for Distribution Circuits"[2] and "Relative Expense for Service Restoration With Different Types of Overcurrent Protection for Distribution Circuits",[3] as presented at the 1942 winter and summer conventions respectively indicate that in the general case single-element fuses provide the major portion of the improvement in service continuity that is obtainable by branch protection. Very little additional improvement can be secured by the use of either reclosing fuses or automatic-resetting reclosers or restorers. The savings in restoration expense were not proportional to the difference in initial cost of reclosing equipment over the low-cost single element fuses. This proportion would be further out of line if the adjustment feature added anything to the cost of the restorer.

It would appear, therefore, that the impracticability of making settings in the field and the application problems involved in co-ordination with fuses located farther out on the line definitely limit the advantage of the adjustability of the restorers' time-current characteristics.

If Mr. Tugby has any suggestions which will assist in making greater use of the adjustment feature of the restorer, they will be of value to those making system-co-ordination studies for overcurrent protection.

REFERENCES

1. TRENDS IN DISTRIBUTION OVERCURRENT PROTECTION, G. F. Lincks, P. E. Benner. AIEE TRANSACTIONS, volume 56, 1937, January section, pages 138–52.

2. RELATIVE VALUE OF DIFFERENT TYPES OF OVERCURRENT PROTECTION FOR DISTRIBUTION CIRCUITS, G. F. Lincks. AIEE TRANSACTIONS, volume 61, 1942, January section, pages 19–25.

3. RELATIVE EXPENSE FOR SERVICE RESTORATION WITH DIFFERENT TYPES OF OVERCURRENT PROTECTION FOR DISTRIBUTION CIRCUITS, G. F. Lincks, C. R. Craig. AIEE TRANSACTIONS, volume 61, 1942, November section, pages 813–21.

E. E. Tugby: Mr. Lincks apparently misinterpreted some passages of the paper when he assumes that it would be necessary to "throw short circuits on a distribution system" to determine restorer adjustments. It is customary to make system short-circuit studies before attempting any kind of co-ordination work, and the computed values are used directly for restorer settings.

On a test setup with five restorers in cascade, all operated 100 per cent correct after having been adjusted by lever setting.

Interpolation should be sufficiently accurate, if three time-current curves are available: namely, maximum, intermediate, and minimum time delay, with calibrated identification of the related points on the time scale.

Selective tripping is assured if time-delay settings between adjacent restorers of the same rating are 45 cycles or more apart. On 30-cycle settings occasional interference was observed because of manufacturing variations.

The assumption made by Mr. Lincks that installation of service restorers is only justifiable in certain highly loaded feeders does not stand up under practical scrutiny. The cost of replacing blown fuses is high no matter what the fuse rating.

Any computation of the economic justification of restorer installation on the basis of feeder size or length or on the basis of service improvement alone, without consideration of maintenance savings, must lead to erroneous results.

No one system condition can be labeled standard, and each requires special study, as any operating man knows only too well. That certain system setups exist which do not justify restorer installations is not disputed. In the majority of cases, however, their installation improves not only service continuity but also reduces maintenance costs, thereby justifying their application even if a moderate revision of fusing practice should be necessary in isolated cases.

History of A-C Wave Form, Its Determination and Standardization

Discussion and author's closure of paper 42-152 by Frederick Bedell, presented at the AIEE Pacific Coast convention, Vancouver, B. C., Canada, September 9–11, 1942, and published in AIEE TRANSACTIONS, 1942, December section, pages 864–8.

P. W. Shill (British Columbia Electric Railway Company Limited, Vancouver, B. C., Canada): Figure 1 of this discussion is a copy of an old oscillogram which is in the writer's possession. It was taken in the year 1908 at the City and Guilds of London Technical College, Finsbury, London, England, by Doctor Silvanus P. Thompson while demonstrating to his class the phase-angle shift of the current with respect to the voltage of a circuit under conditions of bad power factor.

Strangely enough Doctor Thompson was the author referred to by the author of this paper in reference 4.

The oscillogram, as can be seen by a close examination of Figure 1, was made on an "Ondographe Hospitalier." To the best of the writer's knowledge Hospitalier brought out this instrument in 1898. Quite probably it was the "electric curve tracer" which is referred to in the author's reference 17, to which is assigned also the date 1898.

For the benefit of those not familiar with the instrument, a brief description follows: The chart shown in Figure 1 was fastened around a brass drum. This drum was driven at a constant slow speed by a synchronous motor. The motor also drove a gear train which moved a contact around at another constant slow speed. A second contact was attached to the synchronous motor shaft. It can be seen readily that the two contacts would close the instrument circuit instantaneously, once for each revolution of the motor shaft, but never twice in succession at the same point on the a-c wave. This caused a pen, quite similar to the ones we have now on graphic instruments, to move backward and forward across the chart following the successive impulses received from the current through the advancing contacts. The wave traces have the same type of radial distortion with which we are quite familiar today on some types of graphic instruments; so that the chart was printed with one set of co-ordinate lines drawn radially, as can be seen.

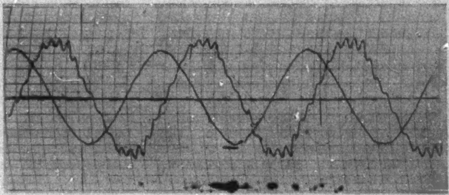


Figure 1. Oscillogram taken in London in 1908 on Ondographe Hospitalier.

This instrument represents an intermediate stage in harmonic analysis, coming as it does between the point-by-point plotting method and the modern reflecting and cathode-ray oscillographs. It was in reality a direct mechanical means of obtaining quickly a trace of the wave form by an automatic point by point record.

In Figure 1 the wave with the very pronounced harmonics is the voltage wave. The almost pure sine wave is the current wave. Time moves from right to left on the chart. These waves were produced from the power supplied to the school by the electricity-supply company operating in that area.

By comparing this voltage wave with one produced by any modern generator, one can readily appreciate the improvement that has been made in generator design, and, therefore, what we owe to the science of harmonic analysis in this field. The year in which the generator which supplied these waves was built is not known to the writer, beyond the fact that it was at some time in the eighteen nineties.

Further, an examination of the general shape of the voltage wave explains why the controversy raged over the so-called "sine-wave assumption." Possibly, when considering the machines in use in the early days, the opponents of the assumption were right. However, one would hardly care to take such a stand today when considering our modern machines. It will also be noticed that if the current wave be corrected for radial distortion it has a more sloping front and a steeper back than a pure sine wave.

Today we are in the habit of considering apparatuses with magnetic cores as producers of harmonics. The current wave was taken from a current passing through an almost pure reactance, as can be seen by the phase angle shift. A comparison of the harmonics appearing in the voltage wave and the current wave would indicate that when the harmonics of a voltage impressed on a circuit exceed a certain magnitude with respect to the fundamental wave, apparatuses with magnetic cores act as chokes or filters for harmonics in the current.

Such wave forms if put out by generators today would certainly cause untold trouble on communication circuits. It so happens that the gentleman who for a time was chief engineer of the telephone company operating in the area which was served by this system was the father of the writer of this discussion. Many a time the father has told his son of the terrible troubles he had with electric-power interference on his system, especially in the days when he was still struggling to try to operate single metallic ground-return telephone circuits.

The writer fully appreciates the great service rendered by the author of the paper to all those seriously interested in the subject of a-c wave form by producing this paper and can only hope that others, with equal knowledge and background in their respective branches of electrical-engineering science as the author has in his, will render the profession equal services by preparing papers of comparable scope and quality. As he says in his concluding sentence, the author has not attempted to tell us the whole story. He has, however, given us references which must prove to be invaluable. Those published from 30 to 50 years ago might otherwise have been in danger of dropping out of sight altogether.

Charles F. Scott (Yale University, New Haven, Conn.): To the significant history which Dr. Bedell recounts he himself has been a notable contributor, as is attested by the 12 references to his own work in the bibliography. To the story of the 1890 period I am able to add a few items relating to the Telluride power transmission and to the determination of wave form. Some of my own work and also that of Lamme and of Mershon are involved.

The wave form of the early commercial alternator with surface winding on a drum armature approximated the sine wave. The first Westinghouse toothed armature (one T-shaped tooth per field pole) produced a wave which caused less transformer-iron loss than that of the smooth armature. I undertook to measure the new wave form—the first such measurement at the Westinghouse factory. I used the contact method but discarded the ordinarily used condenser and ballistic galvanometer and made direct readings by a Cardew (hot-wire) voltmeter. The instrument required normally about a quarter of an ampere for a fair deflection; it took enormously more when there was one contact per revolution of a 12-inch disk, as evidenced by a long soft arc. But the resulting curve was very definite; there was a flat zero for about five per cent of the half cycle and a double peaked maximum with a depressed *V* between the peaks, and the sides were fairly straight lines. The shape of the curve was responsive to the configuration of poles and teeth; thus, as the width of the tooth was greater than the pole, all of the field flux passed into the tooth through a slight angle, and the unchanging flux resulted in a prolonged "flat" zero in the curve. Lamme at once commented that he could change the wave form by reshaping the tooth.

One day Mr. Schmid said to me, "There are two new machines; test them and see what they will do." Then began my real education on synchronous operation; I varied motor field current from normal to zero and then did the same with the generator, observing effects on line voltage and current. I loaded the motor (about 40 horsepower) and made other tests. A few months later, about September 1, 1890, Mr. Schmid said that a 100-horsepower transmission had been sold to L. L. Nunn at Telluride, and we would use two alternators like those made for the transmission from Willamette Falls to Portland, Oreg. My job was to specify the equipment to accompany the two alternators. And without precedent I proceeded to devise a method and apparatus for motor starting and loading, the method of excitation of generator and motor, also to decide upon switches and indicating instruments and prepare instructions for operating a new type of plant. The only justification for some of the complication involved is that it worked. Dr. Bedell quotes P. N. Nunn as saying that the generator and motor were "identical to assure identity of wave form." Possibly, but I never heard of it until Mr. Nunn's explanation 40 years later. The Willamette–Portland and the Telluride transmissions were described by me in the Institute's first paper on Long-Distance Transmission for Light and Power.[1]

R. D. Mershon graduated from Ohio State University in 1890. I attended the commencement, and he told me of his method of measuring wave form. A few months later he developed it at the Westinghouse works. He placed in series with a contact a telephone receiver which responded with vigorous clicks. He introduced a d-c electromotive force which he adjusted until the click was suppressed. The d-c voltage required to just equal the contact voltage was read on a voltmeter. A new brush setting was then made, and the corresponding electromotive force at this point in the alternating wave again read on the voltmeter. This simple and satisfactory method was soon superseded by the oscillograph. Mershon described it in the *Electrical World* (1892, page 425).

Lamme calculated the wave form of the initial Niagara 5,000-horsepower generator and then measured it in a very original way. These he describes in a 12-page paper on "Early Work on First 5,000-Horsepower Alternators," prepared in 1920 at request of E. D. Adams and published in his "Niagara Power."[2] Under "New Method of Design" Lamme relates that I had prepared and showed him an iron filings picture of the magnetic field between the armature and field poles of the machine, made with the aid of a full-sized template. He continues "I believed I could plot the flux conditions by calculation. . . . The plotted field form corresponded to the electromotive force wave for one armature conductor . . . . The wave of other conductors (was the same but) displaced by an amount corresponding to the displacement of the armature conductors themselves . . . . I combined them to get the electromotive force wave of the machine.

"When the machine was set up for test, I arranged to wind a small wire cable around the external cylindrical field frame (about 12 feet in diameter), one end of this cable being attached to a small windlass, which was to be turned at a slow uniform rate to rotate the machine very slowly. The field was magnetized, and a d-c voltmeter was connected across the terminals of the armature winding with a view to taking direct readings at uniform intervals of time during the rotation. If the rotation were slow enough, the voltmeter needle would indicate in succession all the points in the electromotive force wave . . . . The measured wave coincided almost exactly with the predetermined calculation.

"Taking this as a basis, I worked on the method of design during my evenings for the next two or three years . . . . This developed method is the basis of the company's calculation system of today."

Dr. Bedell makes brief reference to William Stanley's pioneer work on the transformer. This is more fully described by Chesney and Scott as part of the Great Barrington demonstration.[3]

REFERENCES

1. LONG-DISTANCE TRANSMISSION FOR LIGHTING AND POWER, Charles F. Scott. AIEE TRANSACTIONS, volume 9, 1892, pages 425–42.

2. NIAGARA POWER, E. C. Adams. Volume II, page 409.

3. EARLY HISTORY OF THE A-C SYSTEM IN AMERICA, C. C. Chesney, Charles F. Scott. AIEE TRANSACTIONS, volume 55, 1936, March section, pages 228–33.

F. Bedell: We are indebted to Mr. Shill for bringing out several interesting points in his discussion. He has emphasized the troubles encountered on early telephone lines because of electric-power interference and has given us a good description of the Hospitalier Ondographe. The curves he has shown well illustrate the great irregularities that existed in the wave forms of early machines, to which attention has been called in the paper. Furthermore, his curves are evidence of the perfection attained in the delineation of wave forms by means of point-to-point curve tracers, which were highly developed and serviceable in their day but were soon outmoded on account of the inconvenience, as well as limited frequency range, of synchronous apparatus.

Irregularities even greater than those shown in these curves were frequently found in early machines. The increase in size of machines, with better design and standardization, has given us wave forms on power systems with remarkably small deviation from a true sine wave, conforming closely to our increasingly rigid standardization rules. One would hardly like to take a stand against the so-called sine assumption today, as Mr. Shill points out.

In the sine-wave assumption, however, it should be kept in mind that it is not assumed necessarily that current and electromotive forces are sine waves. It is assumed that they may be replaced by *equivalent* sine waves having the same rms values, displaced in phase so as to have the same power factor. The vector and circle diagrams based on this assumption have proved their usefulness for fifty years, even with wave forms departing widely from a sine wave. The proof of the pudding is in the eating.

It was early found that certain vector methods (as used, for example, in the once important three-voltmeter and three-ammeter methods for measuring power) were absolutely independent of wave form. It was further found that other methods with which we are all familiar, for example, the determination of performance characteristics by circle diagrams, gave results with engineering accuracy often better in fact than could be obtained by any other method, even though theoretically not free from error.

Such uses of vector and circle diagrams, employed in practically all our engineering projects, are too familiar to need elaboration, but let us take a particular example. In the predetermination of the regulation of a transformer from open-circuit and short-circuit tests, derived by vector methods based on the sine-wave assumption, the regulation is determined *more accurately* than it can be directly measured by commercial instruments. And this, too, in spite of the fact that the primary current of a transformer on open circuit is distorted necessarily, as we well know, by the effects of hysteresis!

An explanation of the fact that irregularities in wave form introduce such small error in vector diagrams based on the sine assumption may be found in the fact that, if fundamental quantities be represented by vectors in a plane, vectors representing harmonics would necessarily be at right angles thereto. This follows from the fact that no harmonic can have an inphase or power component with respect to the fundamental, for currents and voltages of different frequencies represent no power. Harmonics, compared with the fundamentals, are always small and, as the vectors by which they are represented are at right angles to the fundamental plane, their effect is still smaller and merely gives a slight distortion or warping to the plane-vector diagram by an amount too small to have significance. Studies in this direction will be found in reference 12 of the paper. The author has found the representation of alternating quantities by vectors in more than two dimensions useful in some special cases but not generally applicable without difficulty.

Although, as seen above, the validity of the sine-wave assumption and the usefulness of vector and circle diagrams based thereon do not depend upon having perfect or nearly perfect a-c wave forms, our efforts in standardization toward this end will be continued for engineering reasons, such as the effects upon insulation and communication, as discussed in the paper.

Some Air-Blast Circuit-Breaker Installations in Canada

Discussion and authors' closure of paper 42-157 by H. W. Haberl and R. A. Moore, presented at the AIEE Pacific Coast Convention, Vancouver, B. C., Canada, September 9–11, 1942, and published in AIEE TRANSACTIONS, 1942, December section, pages 859–63.

E. W. Knapp (Shawinigan Water and Power Company, Montreal, Que., Canada): It is today a well-established fact that automatic circuit breakers with high-speed and high rupturing-capacity characteristics are required for electric-power systems. The development of air-blast circuit breakers offers considerable promise of adequately meeting these requirements in the future. During the past few years the Shawinigan Water and Power Company has assisted in this development in Canada by utilizing their power system for primary tests during the development period. These tests brought out certain defects and limitations, and, as a result, improvements have been effected. However, it is expected that this equipment will be subject to the usual "growing pains" but will improve with "time and experience." Operating experience to date has also revealed a number of weaknesses, and, in general, the necessary remedial measures have been taken, particularly in those cases where the cause of failure has been established definitely. Normal operating speeds are quite fast, and this feature should prove of considerable value, particularly on power-system networks where transient stability or power-arc damage present problems. It should be noted, however, that although the 69-kv design has been tested to 100 per cent of rated rupturing capacity, the 220-kv design has not been fully tested above 40 per cent of rated rupturing capacity.

Automatic reclosing of power circuits, following transient faults, is playing an important role in power-service improvements. It is quite probable that in the near future, this feature will be considered as an integral part of any system plan, along with relay protections and control. This feature may also be supplemented in special cases with single-phase switching. The air-blast circuit-breaker would appear to be ideally suited to this method of operation, caused by inherently high operating speeds. The authors have attempted to explain briefly a means of adapting the circuit breaker under consideration to automatic reclosing, but I do not fully understand the explanation. It is assumed that "extremely fast reclosing" means 0.35 second or less total time. In this particular design it is not possible to reclose the interrupter contacts while maintaining a high pressure in the arcing chamber. This tends to increase the possibility of restrike across the interrupter contacts, unless the isolator switch is at least partially open before the air blast is stopped. The necessity of operating the isolating switch might result in considerable delay in the automatic reclosing operation, unless the control arrangement is revised. Further explanation of the scheme, together with operating times should prove of interest.

L. B. Chubbuck: For discussion, see item elsewhere on this page.

H. W. Haberl and R. A. Moore: The authors of this paper gratefully acknowledge the interest shown by power-company engineers and especially those who have presented written discussions. A number of interesting points have been brought out by the discussers which we believe should be clarified. As most of the discussion is along parallel lines, our closing remarks will be directed toward the discussion as a whole.

Certain tests reported are referred to as "synthetic tests." They are obviously not synthetic tests. Synthetic means artificial. The word "synthetic" has been applied to the method used by large manufacturing companies to test circuit breakers beyond the short-circuit capacity of the available generating system. In the tests reported by the authors, the kilovolt-amperes interrupted by the breaker were, in every case, identically the same kilovolt-amperes which flowed into the fault from the line and generating system. In other words, the system was carrying a load of several hundred thousand kilowatts, just before, during, and just after the short circuit interrupted by the breaker, the conditions being the same as when a breaker clears a fault in service. There was obviously nothing "synthetic" about such tests.

Perhaps the intention was merely to question how much more interrupting capacity the 230-kv breaker has in excess of 1,000,000 kva interrupted in the tests at 230 kv. The author's paper reports other tests on the single element of the 230-kv breaker interrupting over 5,000 amperes at about 60 kv (phase-to-phase across a 60-kv line carrying full load), such interruption being somewhat over 300,000 kva on the single element. The 230-kv breaker has six of these elements in series per pole, making 18 elements in the three-pole breaker. Multiplying 300,000 by 18 gives 5,400,000 kva. The authors consider these tests to prove adequately that the 230-kv breaker has an interrupting capacity in excess of its rating of 2,500,000 kva.

Pistons and cylinders are mentioned by the discussers as possible sources of trouble in air-blast breakers. Pistons and cylinders operated by compressed air were applied to brakes on railway trains many years ago (by the Westinghouse company). Air brakes are very dependable.

One discusser refers to the noise of air-blast breakers and is concerned about its effect on the nerves of operators. A 230-kv air-blast breaker opening a short circuit makes a noise about like the exhaust of a standard steam locomotive starting a heavy train, which does not appear to bother the crew working within 40 feet of the blast. The operator in a station works at a control board usually 100 feet or more away from the breakers and within enclosing walls. He is not at all disturbed.

The discussers refer to high-speed reclosing in the high-voltage classes of breakers described. The 138-kv breaker will reclose in 20 cycles and the 230-kv breaker in 25 cycles. This timing can be decreased if a specific application warrants. However, the authors feel that if the breaker reclosing time was decreased to less than 15 cycles, additional delay in the form of control would need to be added, as the arc products on transmission-line faults are not blown away in less than that time, and on single-phase switching the arc on the de-energized phase has been known to be maintained for an appreciable time by the induced voltage produced from the sound phases.

To obtain successive shots in air-blast breakers it is only necessary to add storage capacity. Most types of the air-blast breakers described have three operations stored in their individual tanks, and with the aid of storage capacity this number of operations can be increased at will. The moving parts of certain of these air-blast breakers in the extreme high-voltage class are very accessible, and contact inspection on the 230-kv air-blast breakers can be made within 20 minutes. Comparing this with a 230-kv oil-breaker contact inspection time which takes hours, we believe the advantages are in favor of the air breaker.

High-voltage air-blast breakers can be safely mounted indoors where a large quantity of stored inflammable material is a potential fire hazard; this indeed is an advantage for the air-blast breakers.

The arcing time of the air-blast breaker is independent of the magnitude or phase angle of the currents involved. This is indeed an advantage over the modern oil breaker where clearing times in the neighborhood of six cycles can be obtained where the current is of sufficient magnitude to have the deionization or the explosion principle applied. However, on small currents of low power factor such as the charging current of transmission lines the air-blast-breaker arcing time is the same as with the heavy currents. The modern oil breaker under charging current conditions has been known to fail to interrupt where the magnitude of the current is less than the amount required for the explosion or deionization principle to apply.

Design and Operation of High-Voltage Axial Air-Blast Circuit Breakers

Discussion and author's closure of paper 42-155 by Armin K. Leuthold, presented at the AIEE Pacific Coast convention, Vancouver, B. C., Canada, September 9–11, 1942, and published in AIEE TRANSACTIONS, 1942, December section, pages 869–75.

L. B. Chubbuck (Canadian Westinghouse Company, Ltd., Hamilton, Ont., Canada): As the organization with which the writer is connected manufactures both air-blast and oil breakers, the writer feels free to offer a few comments on both types.

Low-voltage air-blast breakers are definitely free of oil hazard, though the high-voltage breakers usually include oil-immersed current transformers in large fragile porcelains.

While failures have occurred in the past with oil circuit breakers, modern oil breakers fully tested and used within their rating have an excellent record under heavy everyday service. Their arc-rupturing devices are very effective, and all interruption occurs quietly within heavy grounded metal tanks. Modern high-voltage and low-voltage oil breakers are very compact, a 15-kv, 1,000,000-kva metal-clad structure running a little over 36 inch centers.

Air-blast breakers have also had their failures. This type of breaker has short *arcing* time, though any longer time extending until the series disconnecting switch opens would be serious. The *over-all* opening time (the important feature) averages a little less than for commercial oil breakers but can be met by high-speed oil breakers.

The opening of an air-blast breaker is accompanied by considerable noise and flame, rather nerve-racking to an adjacent operator.

Because of limited air supply, an air breaker may be safely operated only two or three times successively. The operating high-pressure piping, valves, relays, and cylinders are rather an involved precision equipment and require high-grade maintenance, particularly for outdoor service. Some of these moving parts are rather inaccessible, enclosed in high porcelains. At Arvida the 154-kv breakers are mounted *indoor*.

We note that, because of lack of suitable high-power laboratories, the authors can provide only "synthetic" test proof of their larger ratings, based on a few system tests. We do not consider such data conclusive. During the development of our air-blast breakers, thousands of tests were made on all parts, and complete poles, to full rating, and overvoltage, in a laboratory of ample capacity.

In conclusion, the authors have given us two very interesting and instructive papers covering the development of this new equipment in Canada.

A. K. Leuthold: High-voltage air-blast circuit breakers can be equipped either with, or without, oil-immersed current transformers, but even in the former case, which is often preferred from the economical standpoint, the oil hazard, compared to an oil circuit breaker, is very small, because no arc interruption has to be carried out under oil.

The low-voltage air-blast breakers are also very compact in design. For example: seven-kilovolt breakers require a space from center to center of approximately 34 inches and 13-kv breakers of approximately 42 inches.

Neither with our outdoor nor our indoor type of air-blast circuit breakers can flames be noticed at the breaker exhaust openings. The noise from an air-blast breaker erected outdoors is not objectionably loud, while the noise from a breaker erected indoors is similar to that of a modern high-speed d-c breaker when interrupting a heavy short circuit.

Breakers equipped with automatic reclosing devices can open, reclose, and open a second time in less than 20 cycles without requiring refilling from the main air-storage tank. After two breaker opening operations, automatic refilling takes place from the main storage tank in approximately four to six seconds, and further switching operations can then be carried out.

Experience has shown that the various pistons, applied for the fully automatic operation of the breaker, operate very satisfactorily, even under very low temperature. On outdoor breakers, this piston assembly is carefully protected against snow and rain and, if desired, can be kept above freezing point by means of small heaters similar to those applied on the motor-control pedestals of outdoor oil circuit breakers.

The arcing chambers of high-voltage breakers are designed with inspection openings or other devices, which enable the movable arcing contacts to be inspected, or even replaced if necessary, in a few minutes. At Arvida, the 150-kv air-blast circuit breakers are installed indoors, because they had to replace two oil circuit breakers previously erected in the same building where two 75,000-kva transformer banks are located. This customer preferred to eliminate the oil hazard by applying two air-blast circuit breakers in place of the previous oil circuit breakers.

The high-voltage air-blast circuit breakers equipped with potential-controlled multiple arc breaks, are tested in our large breaker test plant by breaking the partial interrupting capacity with one arc break only, which enables one to judge what the performance of this breaker will be under full-rated interrupting capacity. A definite conclusion can be drawn from these tests because the voltage distribution between the individual arc breaks is equal on account of the potential control of the arc breaks, which is achieved by means of small capacities.

Through a series of developments, the manufacturer has achieved a very high performance from the high-voltage air-blast circuit breakers which, besides the short interrupting time, enables automatic single- or three-phase high-speed reclosure to be carried out.

This insight into the latest breaker developments, including testing and operating experiences, should induce and encourage power companies to collect experience on their own networks by applying this new type of circuit breaker in order to gain valuable operating data for further improvement in the protection and stability of their high-voltage transmission lines and power-distribution systems.

Design, Manufacture, and Installation of 120-Kv Oil-Filled Cables in Canada

Discussion and authors' closure of paper 42-148 by D. M. Farnham and O. W. Titus, presented at the AIEE Pacific Coast convention, Vancouver, B. C., Canada, September 9–11, 1942, and published in AIEE TRANSACTIONS, 1942, December section, pages 881–8.

A. H. Frampton (The Hydro-Electric Power Commission of Ontario, Toronto, Ont., Canada): Under the heading "System," section 2, the authors state that cable connections were used between the step-down transformer terminals and the station bus "as providing a substantial saving in steel structure, greater compactness, and better appearance, and much less exposed high-voltage circuit." In the apparently restricted space available at the site of station *B*, the value of the second and fourth points may be granted. However, it is difficult to accept the inference that the saving of the cost of the additional structures required to accommodate open connections to the transformers had a controlling influence in the decision to use cable at this point. The acceptance of this inference is made more difficult by the assumptions that may be drawn from the statements under "System," section 3, that the line route had to be shortened to approximately one third, and the acquisition of expensive right of way avoided in order to justify the main cable section as against the alternative overhead construction.

It would be interesting if the authors could expand on these points. Perhaps they would be prepared to suggest a rule-of-thumb figure as representing the approximate value of right-of-way saving necessary to justify the adoption of cable constuction.

From Figure 2 of the paper it is presumed that this cable system operates in parallel with 60-kv overhead circuits. From the discussion, it is inferred that varying amounts of power are required to be transmitted between stations *A* and *C*, in order to maintain the most efficient loading on certain generating sources. As these parallel paths would obviously have different impedance characteristics, it would appear that the maintenance of a proper subdivision of load between the parallel paths would occasion difficulties. It would be of interest to know whether any special operating or other procedures have been established in this connection.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): The authors are to be complimented for the thoroughness of their past investigations of design and installation details and of their present investigations of operating matters. If, as in this case, engineers devote some time and efforts to the study of all details, problems relating to oil-filled cable systems become simple.

Records of duct temperatures, and so forth, as they are planned, should be of considerable value in obtaining the maximum allowable load rating for the cable in the actual installation. As load ratings are most important, it would be of interest to have more detailed information given on the methods being used by the authors and on any special points relating to loading.

According to Table I of the paper, 70 degrees centigrade is considered the maximum allowable copper temperature in accordance with the Association of Edison Illuminating Companies specifications. As indicated in my recent AIEE paper on "Load Ratings of Cable—II,"[1] such cable could be operated safely at higher temperatures. If this is attempted, one, of course, must watch out for limitations, such as reservoir capacity or induced sheath potentials.

The Canadian-developed reservoirs shown in Figure 6 of the paper are of interest. The arrangement may be less expensive than those used in the United States. What material is used for the cell walls?

The design of the conductor with flat strands reduces considerably the free oil space between conductor and paper as compared with conductor with ordinary round strands. Have any test data or calculations

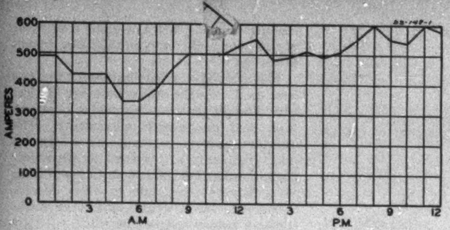


Figure 1. Typical daily load curve for 120-kv cable

Sheath currents not flowing. $T_g = 20$ degrees centigrade

been made on the relation of the maximum stresses for these two types of conductors?

The average coefficients of friction of 0.25 to 0.30 for pulling cable into the ducts are less than usually obtained. We have found higher values in pulling cables into precast-concrete round ducts, using a heavy grease on the cable. While the grease is not a very good lubricant, as demonstrated by our tests, it is used mainly to retard or prevent corrosion of the sheath. It seems to me that the main considerations in determining maximum allowable pulling strains are possible harmful effects on the conductor, on the rest of the material inside the sheath, and on the outer surface of the sheath.

In general, the methods and carefulness used on the whole project described in the paper were apparently of such a nature as to permit high allowable loading and still obtain a satisfactory operating record over a period of decades.

REFERENCE

1. LOAD RATINGS OF CABLE—II, Herman Halperin. AIEE TRANSACTIONS, volume 61, 1942, pages 930–42.

D. M. Farnham and **O. W. Titus:** Mr. Frampton feels that the saving of steel alone would not be a controlling factor in the use of cable from the station bus to the transformer. All projects, of course, contain a number of elements of cost. The sum total of all elements is the determinant. In this instance steel was one substantial element. This was in some measure due to the shape of the premises which made it desirable, even necessary, to place the high-voltage outdoor structure along the south side of the property from east to west, using the entire width. It was, therefore, necessary to place the four transformer banks in a row running from east to west and north of the high-voltage structure. The steelwork required to feed these transformers by overhead construction, plus the cost of the overhead construction, was estimated to be higher than the cost of the oil-filled cables. Part of this advantage may have resulted from a reasonable cable-installation cost, since other oil-filled cables and equipment were being installed at the time in the immediate vicinity. Appearance and "dead-front" characteristics might also be appraised as contributing factors if that were necessary.

Our opinion is that for short runs, as encountered in this case, the problem is worth investigating thoroughly before a decision is reached to use either the overhead or underground method.

Table I. Maximum Allowable Current (Amperes)

Cable (Thousands of Circular Mils)	Allowable Copper Temperature (Degrees Centigrade)	Cables in Duct Bank	Sheath Currents		Ground Temperature (Degrees Centigrade)			
			Flowing	Not Flowing	20	15	10	5
300	70	3	X		382	403	422	442
300	70	3		X	412	434	456	476
300	70	6	X		348	368	387	405
300	70	6		X	372	393	413	432
300	80	6	X		380	398	415	431
650	70	3	X		528	558	585	612
650	80	3	X		576	602	627	652
650	70	3		X	635	670	703	736
650	80	3		X	693	724	754	783

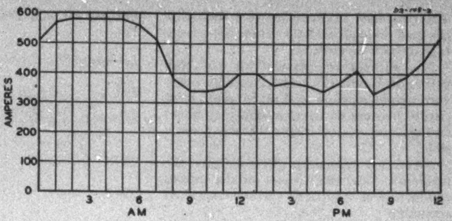


Figure 2. Typical daily load curve for 120-kv cable

Sheath currents flowing. $T_g = 10$ degrees centigrade

As underground costs vary differently from those for overhead with difference in load and raw-material costs, we would not care to set up even an approximate rule-of-thumb figure such as Mr. Frampton suggests, particularly at times such as these.

Mr. Frampton's question as to how the present 60-kv ring system is made to carry its share of load when its impedance is so much greater than that of the cable is well taken.

This difficulty was very cheaply and effectively overcome by splitting the high-voltage bus at station *C* and connecting the cable to one pair of overhead lines and the ring to the other pair. These pairs of lines were paralleled approximately 90 miles away.

Figure 3

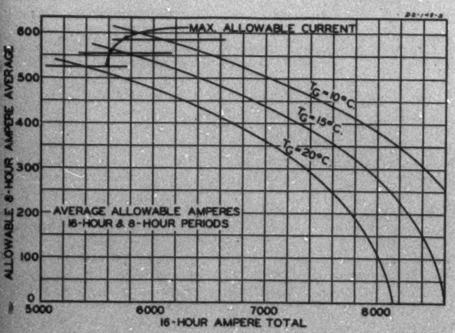
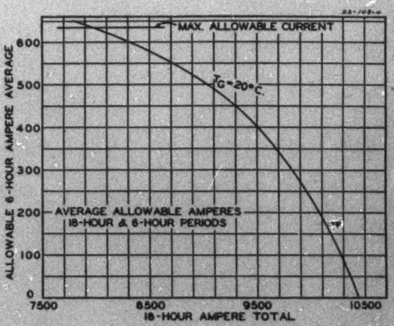


Figure 4



The combined impedances were such that the load divided correctly to take full advantage of both tie circuits.

The authors are grateful for Mr. Halperin's kind remarks. The Canadian reservoirs employed plain steel diaphragms, principally because of wartime difficulty in securing stainless steel within the time permissible.

No tests or calculations were made on the maximum dielectric stresses for the shaped conductors as compared with conventional stranding.

During the development of the pulling eyes, tension tests were made on conductors, paper-wrapped but without the lead sheath. These tests determined both the effectiveness of the pulling eye and the maximum tension on the conductor permissible without permanent deformation. In the field, pulling tensions were kept below these values in all cases. Any added strength caused by the lead sheath was regarded only as added factor of safety.

In replying to the second paragraph of Mr. Halperin's discussion, the listed calculations are samples of those used in establishing the loading schedule of the cables:

Allowable peak load $= I$ at loss factor stated

Loss factor (by design) $= 62^1/_2$ per cent $= \dfrac{(I\,\text{avg})^2R}{I^2R}$

Daily basis 100 per cent loss $\doteq I^2\times R\times 24$

$62^1/_2$ per cent loss factor $=$

$$\frac{\text{avg}\,(I^2)\times R\times 24}{I^2\times R\times 24}=0.625$$

$$\text{or}\ \frac{\text{avg}\,(I^2)}{I^2}=0.625$$

$$\text{or avg}\,(I^2)=0.625I^2$$

For any period of the day
Let

$$Ix^2=\text{avg}\,(I^2)$$

and

$Iy^2=\text{avg}\,(I^2)$ for the remainder of the 24 hours

Then

$$(Ix^2\times X\ \text{hours})+(Iy^2\times Y\ \text{hours})=0.625I^2\times 24$$

or

$$I^2y=\frac{(0.625\times I^2\times 24)-XIx^2}{Y}$$

$$=0.625\times I^2\times\frac{24}{y}-\frac{x}{y}Ix^2$$

Take X as 18 hours and y as 6 hours then

$$\overline{I_6}=\sqrt{(0.625\times I^2\times 4-3I_{18}^2)}$$

(a). Sheath currents flowing, $T_G=20$ degrees centigrade. $T_c=70$ degrees centigrade $-I=528$.

(b). Sheath currents not flowing $T_G=20$ degrees centigrade. $T_c=70$ degrees centigrade $-I=635$.

(For allowable maximum loading see Table I of this discussion.)

For (a) $I_6=\sqrt{(0.625\times 528^2\times 4-3I_{18}^2)}=\sqrt{3(232,320-I_{18}^2)}$. For ($b$) $I_6=\sqrt{(0.625\times 635^2\times 4-3I_{18}^2)}=\sqrt{3(336,021-I_{18}^2)}$.

Curves in Figures 1, 2, 3, and 4 of this discussion are made on the basis of these or similar calculations.

This allows cable loadings to a maximum for 18 hours each day, and from these total amperes a load rating can be had for the succeeding s[illegible]urs, with a resultant loss factor of $62^1/_2$ per cent (see typical loading curves herewith).

This method of scheduling the loading has been used since the cables went into service, with careful attention being paid to temperatures and duct-mouth movements. The idle duct (that is a duct adjacent to a loaded duct) temperature at various locations has been recorded and shows a variation of from $38^1/_2$ degrees centigrade to $40^1/_2$ degrees centigrade, which would indicate a fairly constant copper temperature.

Duct-mouth movements have been recorded both daily and weekly. The daily movements are in the nature of one eighth of an inch or less, while the weekly movement is in the order of three eighths of an inch. Outages or long periods of light loads increase these movements considerably.

The flow of oil to and from reservoirs has been recorded, the daily or even weekly quantity has been approximately one gallon per phase per section, this of course is divided on the two ends. The seasonal variation has so far been only a transfer from cables to reservoirs and has amounted to about 13 gallons per phase per section. This quantity would have to be corrected for the temperature change of the reservoir immersion oil amounting to 0.085 gallon per degree centigrade. The temperature change has been 45 degrees centigrade which is equivalent to 3.8 gallons; thus the actual oil expelled because of seasonal change has been about $9^1/_2$ gallons per phase per section.

Up to the present sufficient power has been handled by using a maximum copper temperature of 70 degrees centigrade. However, if the need arose, there would be little hesitation in increasing this limit to 80 degrees centigrade or even higher. We are now confident that oil demands would not be excessive for the present reservoirs even at extreme temperatures.